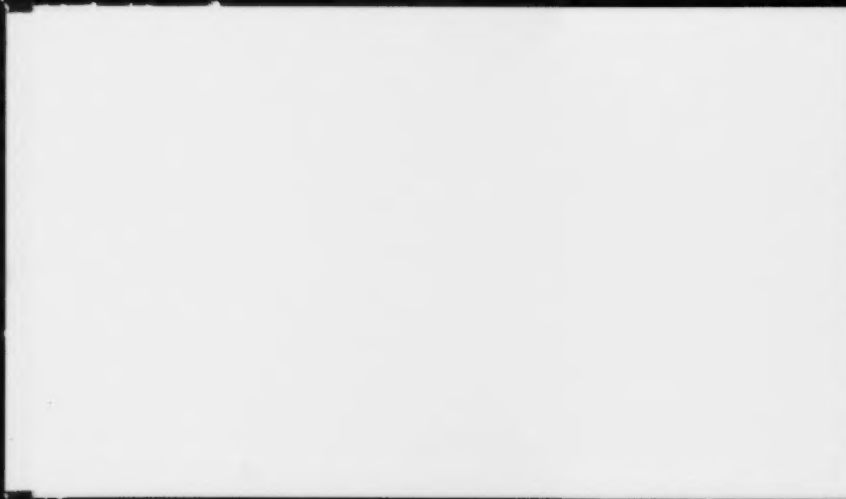

University of Waterloo

Department of

Mechanical Engineering



FINAL REPORT

**STUDY OF FINITE ELEMENT MODELLING OF
MULTIPASS WELDS**

**Kin C. Leung
R. J. Pick**

CONTRACT SERIAL NUMBER OST82-00116
CONTRACTOR: UNIVERSITY OF WATERLOO
REQUISITION NUMBER 23440-2-9060

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**WRI Project No. 208-12
DDS File No. 01ST.23440-2-9060**

**Kim C. Leung
R.J. Pick**

**Department of Mechanical Engineering
University of Waterloo
Waterloo, Ontario
CANADA N2L 3G1**

236 pp

March 1986

**AUG 15 1988
MICROMEDIA**

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Chapter I

INTRODUCTION

The welding process is of technological importance because of its universal use as a fabrication technique. It offers many advantages, such as good joint efficiency, gas and fluid tightness, and economy in construction, compared to other fabrication techniques. However, welding often creates many unwanted problems. One such problem is due to the intense heating of a localized region around the weld, which results in plastic deformation during welding and the formation of residual stresses upon cooling. The presence of residual stress in a welded component may have an adverse effect on the component's life and strength. High tensile residual stresses tend to promote brittle fracture, corrosion cracking and fatigue in welded structures, while compressive residual stresses tend to reduce the buckling strength. Because of these adverse effects, residual stresses have been the subject of numerous experimental investigations and more recently numerical predictions (Appendix A). Unfortunately experimental techniques have proven to be very laborious and are seldom sufficiently sensitive to provide a detailed picture of the residual stresses in a weld. Thus it is seldom possible to predict the change in residual stress if the welding conditions, weld geometry or material are altered.

Fortunately with the development of computers and sophisticated numerical techniques, prediction of residual stresses has become possible without making gross assumptions. Of particular usefulness is

the finite element method which is capable of modelling the complex non-linear mechanisms that lead to the formation of weld residual stresses. However, a survey of the literature (Appendix A) suggests that the application of the finite element technique to the prediction of weld residual stresses is somewhat idealised. Of particular concern is that the majority of investigations are focused on single pass welds while the majority of the weldments are made using multiple weld passes. In those investigations where multi-pass welds have been considered questionable assumptions have been made to simplify the analysis procedures in order to reduce the cost of computation.

In the period November 1982 to March 1986, Energy, Mines and Resources, Canada has supported a study by the authors into Finite Element techniques for the prediction of weld residual stresses with particular consideration of multi-pass welds. This report describes the results of this study.

1.1 STATEMENT OF OBJECTIVES

The initial objective of this study was to develop a finite element model to predict weld residual stress, with special emphasis on multi-pass weldments. A subsequent objective of this study was to investigate how weld passes interact in a multi-pass weld. It was hoped that this study would suggest a simplified but reliable analysis procedure for the prediction of the final residual stress state in a multi-pass weld, without having to simulate the complete thermal and mechanical response of individual weld passes.

The authors proposed the use of a commercial finite element package called ABAQUS [1.1]. It was believed that this code contained most of the necessary features to allow modelling of the complex mechanisms

occurring during the formation of weld residual stresses. This proved to be the case, however, several anomalies were found in the code. These created lengthy delays as it was necessary for the authors of the code to modify the code. In addition certain features of the code proved to be inconvenience or confusing in their use for this application and required special provisions to be developed and inserted in the input data.

One must consider the alternative to the use of a commercial code, that is the development of a code specifically for weld residual stresses. It is estimated that 3 man years would be required to develop such a basic program and therefore there would be little financial benefit. In addition the program would lack many of the convenient features, long term maintenance, high level of support, general availability, documentation and development associated with the major commercial codes. Fortunately the use of a commercial code should greatly assist the "technological transfer" of the university based research to industry since such codes are readily available to or in current use by many industrial firms.

As one objective of this study was to investigate weld residual stress in multipass welds one must comment on the use of an experimental approach. In studying the literature one senses that their techniques are as difficult, if not more difficult, than numerical techniques but with considerably less potential for parametric investigation. The authors do not wish to discount experimental studies but simply state that they did not appear appropriate for this study and therefore have not been pursued. However, in considering the accuracy of the numerical methods adopted and the assumptions made experimental methods can be

used to great benefit and become very desirable. Currently the Department of Energy, Mines and Resources is undertaking such a study and preliminary results will be reported herein. Fortunately results from the numerical study reported can be used in the design of the experiments, hopefully assisting a very difficult undertaking.

1.2 SOURCES OF RESIDUAL STRESSES

According to Macherauch and Wohlfahrt [1.2], the development of weld residual stress can be attributed to three sources; the variation of shrinkage of differently heated areas, surface quenching effects and the transformation of austenite to ferrite, bainite or martensite.

The major source of residual stress is the difference in shrinkage of the variously heated and cooled areas of a welded joint. The fusion zone material originally subjected to the highest temperatures during welding tends to contract more upon cooling but is restrained by the adjacent cooler material. Thus the fusion zone material is subjected to tensile stresses as soon as the material regains its strength upon cooling. Variation in restraint gives transverse residual stresses in the direction normal to the weld of much smaller magnitude than longitudinal stresses in the direction parallel to the weld. This type of shrinkage residual stress is more severe in a material with a large product of Young's Modulus and the Coefficient of Linear Thermal Expansion.

The second most significant source of residual stress is due to surface cooling effects. Because of convection and radiation, surface layers of the weldment, particularly in the highly heated areas, tend to cool more rapidly than the interior material. This subjects the hotter

interior material to compressive loading and yielding if the material strength is low. Upon further cooling, the yielded interior material begins to contract but is restrained by the cooler surface layers. This subjects the surface layers to compressive stresses and develops equilibrating tensile stresses in the interior material. Quenching residual stresses are more pronounced in thick weldments where the maximum temperature difference between surface and interior material is high, and in welding situations where convection and radiation heat losses are significant.

The third source of residual stress is due to transformation strains. During cooling, material heated beyond the A3 temperature during welding is likely to transform from austenite to bainite, ferrite or martensite. The transformation is usually accompanied by an increase in the specific volume of the material. Thus the area being transformed is subjected to compression while tension is built up in adjacent regions that are not being transformed. Because of the gradual cooling of the weld material from the edge of the fusion zone towards the weld centerline, a gradual reduction of tensile stresses towards the centerline is expected. The lower the transformation temperature the larger will be the reduction. In some severe cases even compressive stresses might appear near the weld centerline. To model this transformation effect one would have to predict when the transformation is going to take place and the extent of the transformation [1.3].

1.3 PROBLEM CHARACTERISTICS

To predict welding residual stresses using the finite element technique, it is necessary to approach the problem with a heat transfer analysis and a mechanical analysis. These two field problems are assumed

to be uncoupled so that the heat transfer problem can be solved independently of the mechanical problem. The only link between the two problems is that the temperature results from the heat transfer simulation will be used as the thermal loading in the mechanical response simulation. The necessary assumptions for uncoupling these problems are that the dimensional changes during welding are negligible and that the strain energy is insignificant compared to the thermal energy. Hibbitt [1.4] has found these assumptions reasonable for the modelling of the welding of steel. In the following sections, the characteristics and analysis requirements of these two uncoupled problems will be discussed.

1.3.1 Heat Transfer Problem

During welding electrical power is supplied to the welding electrode to melt the filler metal. The molten weld metal and the accompanying heat content are deposited onto the weldment. The actual heat input received by the weldment is less than the electrical power supplied to the electrode because of heat losses through the electrode surface and the flux. The extent of this heat loss depends on the welding process, power supplied to the electrode, and the type of material welded. Inside the weldment, heat is dissipated away from the weld centerline by conduction alone in the solid region, but is augmented by convection in the molten zone. Because the weld metal and the base plate are subjected to a large temperature range during welding, phase changes and the accompanying absorption and dissipation of latent heats are expected. During cooling, heat escapes through the weldment surfaces to the environment by means of radiation and convection; both are temperature dependent heat transfer mechanisms.

To accurately model the welding heat transfer problem, one would have to include convective heat transfer inside the molten weld pool. Unfortunately the convective heat flow mechanisms in the weld pool are very complex, involving electro-magnetic forces, surface tension effects, and plasma jet forces, to name a few. To include this in the heat transfer model would be a major undertaking and, while desirable, is considered to be only one aspect of the total problem. Therefore a simplified pure conduction heat flow assumption will be made for this study. Since the effect of convection in the molten pool is to transport heat away from the weld centerline to the entire weldment, the additional heat flow in the molten zone can be simulated by defining a higher conductivity in this region in a pure conduction analysis.

Even after ignoring the convective heat flow in the molten metal, the welding heat transfer problem remains very complex and highly non-linear. While the finite element technique is capable of modelling this problem, the major deficiency is the uncertainty in physical parameters and high temperature material properties required for the analysis. In this study, these parameters and properties will be adopted from values reported in the literature. To judge their influence, a parametric study has been undertaken in which the various parameters and properties have been adjusted so as to obtain the temperature output desired. This has led to an understanding of the sensitivity of the solution to the various parameters and will indicate which parameters require careful consideration. It is important to point out, however, that the object here is not to develop an exact model but rather to provide a temperature history of sufficient accuracy for the mechanical analysis, which is the main concern of this study.

1.3.2 Mechanical Problem

The mechanical problem is much more formidable than the heat transfer problem because of the complex thermo-elastic-plastic stress state developed in and around the weld metal during welding. The weldment is assumed to be initially stress free. During welding the weldment is subjected to thermal expansion and contraction strains. These thermal strains can be calculated using the temperature changes calculated in the heat transfer analysis and Average Linear Coefficient of Thermal Expansion data. These strains can be applied to the weldment as a thermal load and the mechanical response to this loading simulated. The final stress state after the material has returned to room temperature will be the weld residual stress. Because of the plastic deformation occurring during welding and the temperature dependent material behaviour, the mechanical response calculations must be carried out in an incremental manner. Creep relaxation during welding is assumed to be negligible because of the short time that the material is at a high temperature (above 0.5 times the absolute melting temperature). A Bauschinger effect is expected to occur in regions adjacent to the fusion zone due to reverse plasticity and therefore must be accounted for in the analysis model. To solve the above problem using the finite element technique, it is necessary to define a constitutive model to describe the material behaviour under the thermal loading. Material properties must also be provided to characterize this model.

The expected difficulties in the mechanical problem are mainly due to a lack of knowledge of high temperature material properties such as the stress-strain and thermal expansion behaviour. An accurate stress-

strain relationship, particularly at high temperatures, is of paramount importance in obtaining an accurate prediction of residual stresses. This is because most of the plastic deformation during welding occurs when the material is hot, and it is this plastic deformation that causes the formation of residual stresses upon cooling. Therefore a critical part of the mechanical analysis will be to obtain accurate and reliable material properties and to determine their influence with a parametric study. Another concern must be the computation cost involved because of the incremental nature of the analysis, especially in the simulation of multi-pass weldments.

1.4 REPORT OUTLINE

As mentioned earlier, the object of this study is to investigate a method for the prediction of residual stresses in multi-pass welds. In Appendix A, a brief literature survey is presented describing previous weld studies and numerical techniques used in modelling the heat transfer and mechanical behaviour during welding.

Initially this study has been concerned with the development of a finite element model for the prediction of residual stresses in a single pass weld. The validity of this model is indicated with a comparison to experimentally measured data. The single pass model for the heat transfer is presented in Chapter 2 and for the mechanical behaviour in Chapter 3. A parametric study included in Chapters 2 and 3 indicates the significance of the various welding parameters, material properties and boundary restraints on the final residual stresses.

The second part of this study, presented in Chapters 4 and 5, is to apply the single pass model to a multi-pass situation. In particular to

a weld with two and three passes. In chapter 4 two weld configurations consisting of two passes have been examined and in chapter 5 a three pass weld configuration was studied. The results from this study will provide information regarding the effects of a new weld bead on the redistribution of residual stresses already in existence from a previous weld pass.

Chapter 6 will discuss the multi-pass results and suggest methods of grouping weld passes so that welding procedures with numerous weld passes can be studied.

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Chapter II

HEAT TRANSFER MODEL OF SINGLE PASS WELD

2.0 INTRODUCTION

To develop a model of a single pass weld it was decided to consider the submerged arc welding of a single bead on the surface of a 316L stainless steel plate of 13.2 mm thickness. 316L stainless steel was chosen because it was believed that its extensive use in the nuclear industry would provide additional knowledge of the high temperature material behaviour, and because in the welding of 316L stainless steel no more than 10% of the austenite is likely to transform into ferrite and therefore residual stresses due to transformations will be negligibly small.

In principle, if not in practice, when a single pass weld can be accurately simulated, a multi-pass weld can be considered as simply the repetition of the process. Therefore considerable care must be taken in undertaking the development of the analysis of a single pass weld. The literature of Appendix A describes a number of studies of single pass welds. Most of these studies often contain assumptions and choices of parameters necessary to allow a solution to the welding model.

Unfortunately experimental results are seldom of sufficient accuracy to prove the correctness of the assumptions, parameters or solution technique chosen. In this study an attempt was made to minimize the number of assumptions and to utilize experimental data in the choice of various parameters.

As described earlier the calculation of the temperature history in a single pass weld can be uncoupled from the calculation of the residual stress. The following section describes the prediction of the temperature history during the single pass welding of the stainless steel plates using the computer code ABAQUS. To check the accuracy of the temperature results, to assist in the choice of values for various parameters and to provide specimens for an experimental study of residual stresses, two stainless steel plates were welded as described in the computer simulation. A summary of the results of the experiment is given in Appendix B. In the next chapter the prediction and measurement of residual stress in these plates is discussed.

2.1 PREDICTION OF TEMPERATURE HISTORY IN SINGLE PASS WELD

The thermal history of a single pass submerged arc weld on a 316L stainless steel plate was predicted using the finite element computer code ABAQUS. This Lagrangian code uses a modified Crank-Nicholson time integration and modified Newton iteration method within each time step to arrive at a solution. A further description can be found in [1.1].

2.2 DIMENSIONALITY OF THE PROBLEM

The heat flow in a weldment during welding is spatially three dimensional. Paley and Hibbert [2.1] and Kou and Le [2.2] have developed three dimensional models however both Muraki [2.3] and Nishida [2.4] have found that the computational cost involved is extremely high. Therefore simplifying assumptions to reduce the problem to two dimensions must be made to reduce costs, particularly if multiple passes are to be considered.

A simplification of the problem can be obtained by assuming that welding has been performed over a sufficient length of time so that a steady state temperature distribution can be observed moving with the heat source (figure 2.1). Under this assumption the heat conduction can be written in terms of the moving coordinates (x, y, ξ) as,

$$\frac{\partial}{\partial x} \left(k \frac{\partial T}{\partial x} \right) + \frac{\partial}{\partial y} \left(k \frac{\partial T}{\partial y} \right) + \frac{\partial}{\partial \xi} \left(k \frac{\partial T}{\partial \xi} \right) = \rho c \frac{\partial T}{\partial t} \quad (2.1)$$

where k = thermal conductivity
 ρ = density
 c = specific heat
 T = temperature
 t = time

$$\text{and } \xi = z - vt \quad (2.2)$$

$$d\xi = -v dt \quad (2.3)$$

Substituting (2.3) into (2.1) results in,

$$\frac{\partial}{\partial x} \left(k \frac{\partial T}{\partial x} \right) + \frac{\partial}{\partial y} \left(k \frac{\partial T}{\partial y} \right) + \frac{1}{v^2} \frac{\partial}{\partial t} \left(k \frac{\partial T}{\partial t} \right) = \rho c \frac{\partial T}{\partial t} \quad (2.4)$$

If the welding speed, v , is high compared with the characteristic diffusion rate of the material, then the third term of equation (2.4), which accounts for the heat flow in the z direction, can be ignored. Thus the problem is reduced to that of determining a two dimensional temperature field at a section normal to the weld line. For lower welding speeds the heat flow in the z direction is significant thus it cannot be ignored completely. Friedman [2.5] has suggested a correction technique for three dimensional heat effects by approximating the third term of equation (2.4) by an equivalent internal heat generation term in the formulation, so that a two dimensional analysis can still be carried

out. In the study being reported the two dimensional heat flow assumption has been made, however a three dimensional heat source has been assumed to move through the section of analysis to account for the heat flow in the z direction.

2.3 HEAT INPUT MODELLING

2.3.1 Weld Efficiency

The most critical input data required for the welding heat flow analysis is the actual power, Q , that enters the weldment. This power is usually expressed in terms of the voltage, V , and current, I , supplied to the welding electrode as,

$$Q = \eta V I \quad (2.5)$$

where η is the arc efficiency representing the ratio of actual power entering the weldment to the power supplied to the arc. Submerged arc welding has been adopted since heat losses through radiation and convection from the weld pool are minimized and held reasonably constant by the flux. While the flux can be included in the finite element model the unknown thermal properties of the granulated blanket would make any result questionable. Therefore the authors have chosen to use a weld efficiency, η , to account for heat losses through the flux. This technique of modelling heat losses through the flux is by no means correct as it assumes that the heat losses occur instantaneously rather than gradually over the entire cooling period. Nevertheless, it is a convenient way of modelling these losses in a finite element heat flow analysis. This approach has been used by Andersson [2.6] in his

analysis of heat flow during submerged arc welding of mild steel plates with good results.

The sensitivity of the predicted temperatures to changes in arc efficiency has been studied by the authors [2.7]. The results show that the peak temperatures at locations in and around the weld pool change approximately the same amount as the change in the arc efficiency. This illustrates the significance of arc efficiency in obtaining an accurate temperature distribution especially inside the weld pool where the temperature is high.

An arc efficiency of 99% for SAW welding of steel was first quoted by Rykalin [2.8] in 1957. Christensen and co-workers [2.9] measured arc efficiencies for SAW welding of mild steels at various arc energy levels between 2000 to 10,000 cal/sec and reported a band of efficiencies between 90 and 99% (figure 2.2). Similar experimental data for 316L stainless steel is not available in the literature, but η is believed to be slightly lower than that of a mild steel plate welded under similar welding conditions. This belief is based on the fact that the thermal diffusivity of stainless steel is about 1/3 that of mild steel, which means that it is relatively easier for heat to escape through the flux during welding of a stainless steel plate and hence a lower arc efficiency is expected.

Because of the lack of data, a semi-empirical technique was developed by Lemczyk [2.10], under the supervision of the authors, to estimate the arc efficiency of 316L stainless steel plate during submerged arc welding. Initially, to verify the method, mild steel plates were submerged arc welded under the same condition as the stainless steel plates used in the welding test (Appendix B). The test gave

an arc efficiency of 0.93, which is within the range of Christensen's results. Applying the same technique to the submerged arc welding of 316L stainless steel plate gave an arc efficiency of 0.90. This result coincides with the prediction of a lower arc efficiency for submerged arc welding of stainless steels and therefore an arc efficiency of 0.90 was adopted in the heat flow analysis of the submerged arc welded stainless steel plates.

2.3.2 Heat Source Model

Of equal importance to the actual power supplied to the weldment is the distribution of power. The choice of power distribution will greatly affect the weld pool shape and hence the temperature distribution in the vicinity of the weld. The authors investigated a number of heat source models which are summarised below and described in some detail in Appendix C.

Pavelic [2.11] first proposed that the power source should be a circular Gaussian distribution of thermal flux deposited on the surface of the weldment. This circular heat source was used by Kou [2.2] in a 3D GTA weld analysis and later modified by Friedman [2.12] so that it can be applied to a 2D analysis. Another proposed model distributes the power uniformly over the fusion zone elements as the welding arc passes. This approach was first used by Paley and Hibbert [2.1] in a finite difference analysis. They found that this model tended to overestimate the fusion zone size. This indicates that a Gaussian type of power distribution within the fusion zone is more appropriate. Goldak et.al. [2.13] have developed such a distribution model by assuming a Gaussian heat flux distribution in the weld pool simulated by two ellipsoids (see figure 2.3). This model appears to have the greatest potential of

accurately simulating the temperature distribution in the fusion zone of a weld particularly in a 2D analysis. This is because the convection and conduction heat flow in the longitudinal direction, which is ignored in a 2D analysis, can be approximated by properly distributing heat fluxes into the two ellipsoids. In addition Goldak's model allows the heat distribution to be changed by the simple change of various geometry parameters rather than changes to the finite element mesh. This is particularly attractive in considering multi-pass welds and therefore this model has been adopted for this study.

The double ellipsoidal Gaussian distribution assumed for the heat source consists of two quadrants; a front quadrant which simulates the 3D heat effect ahead of the weld and contains 80% of the net heat input from the arc; and a rear quadrant which simulates the convective heat flow towards the tail of the weld pool and contains the remaining 20% of the heat content. The parameters which are required to describe the geometry of the two ellipsoids were interpreted from the experimentally measured weld pool shape (see Table 2.1).

2.3.3 Convective Heat Flow in the Weld Pool

The thermal energy deposited into a weld pool is transported away by two heat transfer mechanisms, namely conduction and convection. Conduction is driven by the temperature gradient in the molten metal while convection is driven by the fluid flow inside the weld pool. For low penetration welds, fluid flow in the weld pool is usually small and can be ignored. For deeper penetration welds such as in submerged arc welding, with weld depth to width ratio approaching or exceeding one

half, the role of convective heat flow as a heat transfer mechanism becomes significant.

There are many ways of modelling convective heat transfer inside the weld pool. The most complicated one is to perform a convection-diffusion type of heat flow analysis which requires the velocity distribution in the weld pool as input data. Unfortunately, the velocity field in the weld pool is very difficult if not impossible to measure experimentally. Numerical modelling of the velocity field is also very difficult because of the number of physical phenomena involved, such as magnetic forces, arc forces, surface tension and gravity, to name a few.

A second method of modelling convective heat transfer inside the weld pool ignores the physical phenomena, estimating the weld pool shape from the measured weld width and ripple length, and calculating the temperature distribution in the solid material using the melting temperature as a boundary condition. Investigators such as Pavelic [2.11] and Tsai [2.14] have shown good correlation between experiments and numerical results using this technique. The advantage of this method is that the net heat input to the weldment is not required and the latent heat of fusion can be ignored in the analysis.

A simpler approach commonly used by most researchers is to compensate for the convective heat transfer with an artificial increase of conduction heat transfer by defining a conductivity k for the molten metal an order of magnitude higher than its value at the solidus temperature. This approach has been used by Friedman [2.15], Brody [2.16] and Papazoglou [2.17]. The difficulty associated with this technique is that various values of conductivity for the molten metal

must be tried until a reasonable temperature distribution inside the weld pool is obtained. It was found in an initial study of the stainless steel plate that using a value of k five times its value at 1375°C gave a good temperature distribution in the weld pool. Hence this value of k was assumed in further heat flow analyses.

2.4 HEAT LOSS MODELLING

On the exposed surfaces of the weldment, heat exchange takes place with the surrounding air in the form of free convection and radiation (see figure 2.4). Free convection dominates the heat loss at the lower temperatures while radiation dominates at higher temperatures. The portion of the weldment surface covered by the granulated flux during welding is assumed to be insulated. As discussed earlier the heat loss through this region is accounted for by the arc efficiency parameter.

2.4.1 Radiation

Heat loss due to thermal radiation is significant in the vicinity of the weld pool where the temperature difference between the weldment and the environment is high. The weldment surface emits radiant energy solely by virtue of its temperature and at the same time absorbs radiant energy emitted by the environment. The resulting net heat loss due to radiation can be described by the Stefan-Boltzman equation as,

$$q_{\text{rad}} = \sigma \epsilon F (T_s^4 - T_r^4) \quad (2.6)$$

where q_{rad} = net radiant heat loss from the surface

σ = Stefan-Boltzman constant ($5.67 \times 10^{-8} \text{ W/m}^2 \text{ }^{\circ}\text{K}$)

ϵ = surface emissivity

F = shape factor (1.0 for flat plate exposed to a semi-infinite space)

T_s = surface temperature

T_r = environment temperature (20°C)

The emissivity ϵ of equation (2.6) can vary between zero and 1.0 depending on the temperature, roughness and the extent of oxidation on the weldment surface. Therefore, in order to model the radiant heat loss from the weldment accurately the surface emissivity of the 316L stainless steel plate used for the welding test must be determined.

A survey of the literature showed that no emissivity values are quoted for a 316L stainless steel, but data for a 316 stainless steel is found in Goldsmith [2.18]. This emissivity data for 316 stainless steel, shown in figure 2.5, has been adopted for the current investigation. In this figure it can be seen that during the initial heating of a 316 plate, in the as received condition, the emissivity ϵ maintains a constant value of 0.3 up to a temperature of 300°C. As the temperature increases further the emissivity begins to rise, possibly due to the formation of an oxide film on the plate surface. When the plate is heated to a maximum temperature of 950°C an emissivity level of 0.68 is recorded. Upon cooling, the surface emissivity drops to a level of 0.58 at 240°C instead of 0.3. This indicates that once the oxide film is formed on the plate surface, its effect on emissivity remains during subsequent cooling or reheating of the plate.

As mentioned in the weld test (Appendix B), the 316L plate specimens were initially heat treated at 900°C for 4 hours to relieve all residual stresses. This heat treatment cause the formation of a grey oxide film on all plate surfaces. It can be estimated that these oxidized surfaces should have a maximum emissivity of 0.6 at 900°C and drop to a value of approximately 0.5 at 250°C during the welding test. This is shown on

Figure 2.5. Because of this small difference in the predicted emissivity between 250°C and 900°C and the increased computing time involved in modelling temperature dependent values of ϵ , an average surface emissivity of 0.55 has been assumed in the heat flow analysis.

As a check, two heat flow analyses were conducted, one assuming no radiation from the weldment surface and the other assuming a surface emissivity of 0.55. The results indicated that the temperature distributions are not greatly affected by neglecting of radiation. This is expected because during SAW welding the high temperature region near the weld pool is covered by the flux blanket and assumed to be insulated. For other arc welding processes, however, the effect of radiation is expected to be much more significant.

2.4.2 Free Convection

The heat loss by free convection from the weldment surfaces can be modelled by the Newton's cooling law as,

$$q_{\text{conv}} = h_c (T_s - T_r) \quad (2.7)$$

where q_{conv} = heat loss by free convection

h_c = heat transfer coefficient

T_r = environment temperature

T_s = surface temperature

The coefficient h_c in equation (2.7) is usually a temperature and geometry dependent parameter.

Numerous experiments [2.19,2.20,2.21] have been conducted in the past to determine the convective heat transfer coefficient h_c for isothermal horizontal surfaces. The results show that h_c can be

correlated by equations of the form,

$$Nu = \frac{h_c L}{k_a} = C (Gr Pr)^n \quad (2.8)$$

where Nu = Nusselt number

k_a = conductivity of air

Pr = Prandtl # of air

Gr = Grashof # of air

L = characteristic dimension of plate surface

C and n = are the constant and exponent obtained from experiments

and k , Pr , Gr are evaluated at temperature $(T_s + T_r)/2.0$

For an upward facing heated isothermal surface, the most representative values of C and n are 0.162 and 0.33 respectively, according to Yousef's [2.19] experimental results. For a downward facing surface, the values of C and n are both 0.25 based on Fishenden et.al.'s [2.20] results. Using the above values for C and n , the heat transfer coefficient h_c as a function of temperature can be evaluated for both the top and bottom surfaces. The results are presented in figure 2.6.

From these results it can be concluded that the heat transfer coefficient h_c should be less than $10 \text{ W/m}^2 \text{ } ^\circ\text{K}$ in the range of temperatures the exposed weldment surface is subjected to during welding. Yet assuming this h_c value for both the top and bottom surfaces in the heat flow analysis resulted in a cooling rate much slower than that observed experimentally. This indicates that the assumed h_c value was too low for modelling convective heat loss from non-isothermal surfaces. In fact a higher h_c value is expected for non-isothermal surfaces as the buoyancy force driven flow normal to the surface is augmented by the

pressure gradient driven flow along the surface caused by the non-uniform air temperature distribution. Yousef [2.19] has reported such a pressure gradient driven flow near the edges of an isothermally heated plate which results in heat transfer coefficients along the edge almost double the interior value. Unfortunately correlation equations for heat transfer coefficient of non-isothermal horizontal surfaces are not available. Therefore for the heat flow study the authors conducted a heated plate test (Appendix D) in order to establish a semi-empirical equation for the heat transfer coefficients over non-isothermally heated surfaces such as weldments. For upward facing surfaces the values of heat transfer coefficient shown in figure 2.7 have been adopted. For downward facing surfaces 75% of the upward facing values have been assumed.

2.5 LATENT HEAT EFFECT

It was assumed that during welding of 316L stainless steel plates only one transformation, the solid austenite phase to the liquid phase transformation, occurs in the fusion zone between 1375°C and 1400°C [2.22]. The melting and resolidification of the weld metal is accompanied by the absorption and liberation of the latent heat of fusion. The absorption of latent heat during the heating stage results in a delay of the advancing melt front while the liberation of latent heat during the cooling stage causes a delay in the collapsing solidification front. Thus including a latent heat effect in the heat transfer model will result in smaller fusion zones but longer weld pools. The peak temperatures in the weld pool will also be reduced as a portion of the heat input is required for the melting of the metal.

The latent heat effect is accounted for in ABAQUS by an artificial increase of the specific heat value over the melting temperature range as:

$$C^*(T) = C(T) + \frac{H^*}{(T_L - T_S)} \quad T_S < T < T_L \quad (2.9)$$

where $C(T)$ = specific heat value at temperature T

$C^*(T)$ = the modified specific heat value at temperatures $T_S < T < T_L$

H^* = latent heat of fusion

T_L = liquidus temperature (1400°C)

T_S = solidus temperature (1375°C)

The exact value of H^* for 316 or 316L stainless steel has not been found in the literature. A value of 292 J/g has been assumed for 316L. This value was estimated by adding the latent heats of fusion of individual elements (shown in Table 2.2) in the ratio of their percent content.

A problem associated with the modelling of latent heat effects is the discontinuous temperature gradient at the solid/liquid interface. This requires the use of elements which utilize a trapezoidal numerical integration scheme inside the fusion zone so that the non-smooth temperature distribution can be simulated. In the ABAQUS program the 4 noded isoparametric elements use a trapezoidal integration while the 8 noded isoparametric elements use a Gaussian integration. Thus the 4 noded elements must be used in the region where melting is expected.

2.6 MATERIAL THERMAL PROPERTIES

The calculation of the temperature distribution during welding requires a knowledge of the thermal properties of the parent plate and

the weld metal, specifically, the conductivity (k), specific heat (c) and the density (ρ). In the example being studied the parent plate material is 316L stainless steel which has the typical composition listed in Table 2.3. The weld metal is ER 316L which has a slightly lower carbon content and an addition of 0.14% niobium. Because the difference in composition of 316L and ER 316L is small and a 46% dilution of the weld metal occurs, it is reasonable to assume that the weld metal has the same thermal properties as the parent plate material.

The thermal properties mentioned above usually change considerably over the wide range of temperature that the material is subjected to during welding. Data regarding temperature dependent properties for a material of specific composition is seldom available especially at the high temperatures encountered during welding. Because of this, researchers concerned with welding heat flow problems have, in the past, approximated elevated temperature properties by extrapolation of the available low temperature data, or used high temperature properties of other materials of similar composition.

A literature survey has revealed that the temperature dependent thermal properties of 316L stainless steels are not available. Instead, the properties of a 316 stainless steel which has a very similar composition were found in Touloukian [2.23] and Goldsmith [2.18]. It is believed that the two steels have very similar thermal properties because their compositions are similar except there is 0.08% carbon in 316 as compared with 0.03% carbon in 316L. Therefore the thermal properties of 316 stainless steel have been assumed for the heat flow analysis and their temperature dependency will be described below.

2.6.1 Conductivity (k)

The thermal conductivity of 316 stainless steel [2.23] is shown in figure 2.8 with data available up to approximately 1200°C. The conductivity values between 1200°C and the solidus temperature (1375°C) as indicated by the dashed line was assumed by extrapolation. Above the solidus temperature the thermal conductivity of the molten metal is expected to be lower than its value at the solidus temperature. However, as described earlier k has been assumed to be 5 times higher than its values at the solidus temperature in order to simulate the convective heat transfer mechanism in the molten pool.

2.6.2 Specific Heat (c)

The specific heat of 316 stainless steel was found in Touloukian [2.23] and is shown in figure 2.9. Again the data is only available up to approximately 1200°C and extrapolation of the existing data is assumed to obtain specific heat values up to the solidus temperature (1375°C). It is reasonable to assume that the specific heat values above the melting temperature have the same value as the specific heat at the solidus temperature.

2.6.3 Density (ρ)

The density of a 316L stainless steel is approximately 7.952 g/cm³ at room temperature as given in Goldsmith [2.18]. At elevated temperatures the material usually expands resulting in a lower density. In an uncoupled heat transfer analysis, however, the material is assumed to occupy the same volume throughout the analysis. Therefore, a constant density of 7.952 g/cm³ has been assumed for all temperatures in the heat flow analysis.

2.7 COMPARISON OF EXPERIMENTAL AND FINITE ELEMENT ANALYSIS RESULTS

2.7.1 Weld Model

The single pass submerged arc welding experiments described in Table 2.4 and Appendix B were simulated using the ABAQUS finite element program. A two dimensional analysis was performed along a plane perpendicular to the weld (see figure 2.10). Because of symmetry along the weld centreline only one half of this plane was analyzed. This plane was discretized into a total of 111 elements and 260 nodes as shown in figure 2.11. Sixty five, small, linear interpolated, 4 noded iso-parametric elements were used in the fusion region in order to model the latent heat effect (see Region I, figures 2.12). Larger quadratic interpolated, 8 noded iso-parametric elements were used for the remaining 46 elements (Regions II, III and IV, figures 2.13, 2.14, 2.15). Along the interface between the 4 noded and 8 noded elements, the boundary interpolation of the 8 noded elements was reduced to linear to avoid incompatibility problems. An element thickness of 1 cm was also assumed for all elements.

2.7.2 Finite Element Analysis Results

The heat flow in the weldment during SAW welding was analyzed over a period of 60 minutes with time $t=0$ corresponding to the time when the weld pool front first reached the plane analysis. An automatic time stepping algorithm was used in which the program optimizes the time step size to satisfy a user defined convergence tolerance and allowable temperature change per time step. In this study, the convergence tolerance was set to 5°C and the allowable temperature change per time step to 500°C for the initial 5 minutes of the analysis. It was reduced

to 2°C and 200°C respectively for the last 25 minutes of the analysis because of the lower cooling rate involved. The entire analysis over the 60 minute period took approximately 2 hours CPU time on a VAX 11/750 computer. The input data required by the ABAQUS program for this study is presented in Appendix E. The results of the analysis will be described in the following paragraphs.

Figure 2.16 shows the temperature history, as predicted by the finite element analysis, at the 10, 20, 30, 40 and 60 mm locations on the top surface of the plate. Experimental temperatures from Appendix B at the same locations were also plotted on this figure for comparison. It can be concluded from this plot that the predicted temperatures are approximately 10 to 20°C lower than the experimental ones, however, the cooling rates and the general behaviour of the temperature history are similar.

Figure 2.17 shows the predicted temperature history at 0, 10, 20, 30, 40 and 60 mm from the weld on the downward facing surface. No comparison to experimental data can be made as no temperature data was recorded for the bottom surface. However, a comparison with the numerical results of the top surface reveals that the predicted temperature history on the top and bottom surfaces are almost identical at locations beyond 20 mm from the weld centreline. At closer locations the top surface temperature becomes substantially higher, which corresponds to the fact that the weld bead is located on the top half of the plate.

Figure 2.18 shows the temperature history at 3 locations inside the fusion zone. Distinctive discontinuities in the curves can be observed between 1375 to 1400°C during the heating stage at all three locations. This is believed to be due to the absorption of the latent heat of fusion upon melting of the weld metal. This latent heat is later

dissipated by the molten metal upon resolidification as indicated by the plateaus on the temperature history curves. The duration over which the effects of latent heat take place is much longer during the cooling stage than the heating stage. This is possibly due to the lower temperature gradient near the tail end of the weld pool. A peak temperature of 4700°C can be observed at $t = 0.2$ seconds which is the time when the welding arc passes the plane section of analysis. This peak temperature is unrealistically high as the boiling point of 316L stainless steel is estimated as 2800°C using vapour pressure equations reported by Kim [2.24]. It can also be observed from this figure that the melting and subsequent resolidification of the weld metal takes place in less than 3 seconds.

The temperature contours along a plane perpendicular to the weld at different times during the welding simulation process are shown in figures 2.19 to 2.24. These figures show the gradual development of the weld pool cross-section as welding progresses. The maximum fusion zone size is observed at time $t = 0.536$ seconds (figure 2.23) which is slightly smaller than that measured experimentally (see figure 2.25). The weld pool shape along a plane parallel to the weld can also be constructed from the contour plots and the result, as shown in figure 2.26, indicates that the weld pool length is slightly less than 4 cm. This agrees well with the experimental weld pool length estimated from the weld bead ripples.

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Chapter III

RESIDUAL STRESS PREDICTION IN SINGLE PASS WELD

3.0 INTRODUCTION

As described earlier the single pass weld analysis has been uncoupled into a thermal analysis which resulted in the temperature history described in the previous chapter and a stress or mechanical analysis to be described in this chapter. The computer code ABAQUS [3.1] has been used to determine the residual stresses in the welded stainless steel plates described in Chapter 2 and Appendix B. Energy, Mines and Resources, Ottawa has undertaken the experimental measurement of the residual stress on these welded plates and preliminary results are reported herein and compared to the finite element predictions. A parametric study of the effects of restraint conditions on the weld residual stress has also been undertaken and the results included in this chapter.

3.1 ANALYSIS TECHNIQUE

The thermo-elastic-plastic model in the ABAQUS code was used in the prediction of weld residual stress due to welding. A general description of the analysis technique is presented here. A complete description of the model can be found in the ABAQUS theory manual [3.1]. The input data required by the ABAQUS code for the residual stress analysis is presented in Appendix E.

In the finite element analysis the weld model is represented by a series of equilibrium equations which may be written symbolically as:

$$\bar{F}^N(\bar{u}^P) = 0.0 \quad (3.1)$$

As the problem is history dependent the equilibrium solution is solved in a series of small increments using Newton's method of iteration. The residual stresses are arrived at by solving these equations at each increment from the start to the finish of the welding process. A time independent plasticity model with a kinematic hardening assumption has been adopted to characterize the material behaviour during welding. Time dependent creep effects are not included because of the short time the material spends at high temperature during welding. The kinematic hardening theory has been adopted as the simplest theory that can model the reverse plasticity and Bauschinger effect that is expected to occur during welding. The kinematic hardening theory used by ABAQUS is simplified by assuming that the material behaviour in the plastic range can be linearized (constant work hardening slope).

As mentioned to accomodate the history of the kinematic and material non-linearities the solution to the problem is obtained using an incremental approach. Within each time increment the temperature data from the heat transfer analysis is read in and the following solution algorithm is then used to obtain the solution:

1. Form the material stiffness H_1 based on the state variables at the end of the last iteration ($H = D$ if $i = 1$, where D is the elastic stiffness matrix and i is the number of the equilibrium iterations).

2. Update the nodal displacements ($\bar{u}^P$) by solving equations (3.1) using Newton's iteration method (outer iteration loop),

$$\bar{u}_{i+1}^P = \bar{u}_i^P + \Delta \bar{u}_{i+1}^P \quad (3.2)$$

3. Integrate the basic state variable, $\underline{\beta}$, using Newton's iteration approach (inner iteration loop),

$$\underline{\beta}_{k+1} = \underline{\beta}_k + \Delta \underline{\beta}_{k+1} \quad (3.3)$$

If converged $\underline{\beta}_{k+1}$ will be the radius stress matrix $\underline{\beta}$ after $i+1$ equilibrium iterations (k is the stress integration iteration number).

4. Update other state variables associated with the kinematic hardening theory.
5. Calculate the residual force vector $\bar{F}^N(\bar{u}^P)_{i+1}$ after iteration $i+1$.
If the residual force at any node is larger than the prescribed tolerance the above procedure will be repeated. If not, the next temperature load increment will be applied and the above procedures will be repeated starting from iteration $i=1$.

In the current study a prescribed force tolerance value of 10.0 N was used. This value was chosen after a series of trial runs and was found to give acceptable convergence of the force residuals and displacements. It should be pointed out that this force tolerance must be adjusted to coincide with the yield stress values adopted for temperatures above melting so that convergence of the displacement solution can be achieved.

The temperature history from the heat transfer analysis was followed exactly except at temperatures above melting. Temperatures above melting

can reach the boiling point (2800°C) but at these temperatures deformation due to thermal expansion will not contribute to the residual stresses due to the low stiffness of the material. However such high temperatures do create numerical problems because of the low material stiffness assumed for the molten metal. Therefore those temperatures which exceed 1400°C have been reduced to 1400°C in this study. An initial study comparing the results of two analyses one with modification of the peak temperature to 1400°C and one without, found that the residual stresses predicted in the two cases are almost identical but the one without the peak temperature modification required slightly longer CPU time.

The time incrementation scheme in the stress analysis is independent of that in the temperature analysis thus interpolation of the temperature data was necessary. There is considerable variation in the rate of temperature change between the start and finish of a welding process. During the initial heating period the temperature change is rapid thus a small time increment is necessary, while the temperature change during cooling is slow and a larger time increment can be used. In the present study an automatic time incrementation scheme was adopted whereby the program adjusts the time increment size based on the solution convergence rate.

During this study it was found that the original version of the Code ABAQUS contained several formulation errors which became significant in this analysis. These problems have been reported in progress reports [3.2] and [3.3]. These errors were corrected by Hibbitt, Karlsson and Sorensen Inc., the authors of the code but resulted in considerable delay in the project.

3.2 WELD MODEL

The distribution of thermal transient stress in a weldment during welding and the residual stress upon cooling are three dimensional in nature. But to conduct such a 3D welding stress analysis would be prohibitively expensive. Fortunately with the welding of flat plates, the 3D stress analysis problem can be reduced to two dimensions by analyzing a section normal to the weld line (see Figure 3.1). This section is chosen at the middle of the weld so that starting and stopping effects of the weld can be ignored. A plane strain condition has been assumed which implies that the section of analysis remains plane throughout the welding process. This assumption is adequate in regions removed from the weld pool but it may not be valid in the fusion zone when the material is molten [3.4]. As a result accurate calculation of deformation in the fusion zone is not possible. Nevertheless it allows weld residual stresses to be calculated because they are not developed until the molten metal resolidifies, at which point the plane strain assumption is valid.

In the literature it was found that a majority of researchers assume a zero plane strain condition. This boundary condition only allows the modelling of in-plane distortions as shown in figure 3.2 ,but not out-of-plane distortions such as bowing and longitudinal shrinkage shown in figure 3.3. To overcome this problem, pseudo three dimensional analyses have been undertaken [3.5] using a layer of three dimensional solid elements instead of two dimensional plane strain elements. Keeping plane section plane and allowing one face of the solid elements to rotate or translate relative to the other will model the out-of-plane

distortions. However such a model will increase the number of nodes by 100% to 150% with an even greater increase in computing cost.

In this study two dimensional generalized plane strain elements were used. The elements were assumed to be bounded by two initially parallel planes. These bounding planes are allowed to translate or rotate relative to each other with three degrees of freedom as shown in figure 3.4. This effectively adds two extra nodes to each element to accomodate the three extra degrees of freedom. However as the same extra nodes can be used in all the other elements bounded by the same two planes the increase in the total number of nodes will only be two. The three degrees of freedom Δu_z , ϕ_x and ϕ_y can be included or removed from the generalized plane strain model to simulate different plate restraint conditions during welding. If all three degrees of freedom are restrained, then the model returns to that with a zero plane strain assumption.

For the single pass weld described in Appendix B, the plate was secured onto the welding table by two clamps along one side of the plate only (see figure 3.5). This restraint condition is probably sufficient to prevent any rotation of the plate about the x axis (bowing) but it would not be capable of stopping the plate from sliding under the clamps (longitudinal shrinkage) due to thermal expansion and contraction during welding. Also the rotation of the plate about the y axis will be negligible at a section far from the ends of a weld. Thus in this analysis the only degree of freedom allowed is the translation in the z direction (Δu_z), while rotation about the x and y axis are constrained to be zero.

Because only one-half of the plate was secured onto the welding table while the other half is free to rotate during welding the displacement field is non-symmetrical about the weld line. To reduce the cost of computation, however, symmetry about the weld centreline is assumed so that only half of the plate section needs to be considered in the weld model. This assumption of symmetry should not affect the predicted stresses and strains, but the calculated displacements at the free half of the plate will be approximately one-half that of the measured values.

During the heating stage of the welding process the free half of the plate tended to rotate downwards but was prevented from doing so by the welding table. Upon cooling the free half of the plate rotated upwards and separated from the table. In the weld model the welding table is simulated by a rigid block with a large Elastic Modulus. When the plate and the rigid block come in contact, the contact surfaces are assumed to be frictionless so that only forces normal to the surface can be transmitted across the interface. A symbolic representation of the in-plane kinematic constraints is shown in figure 3.6.

3.3 FINITE ELEMENT MESH

The finite element mesh used in the heat transfer analysis was not used here because of a element compatibility problem existing in the transition region between the 4 noded and 8 noded elements. The oscillation of longitudinal stress immediately outside of the fusion zone reported in [3.3] can be attributed to this problem. Instead a new mesh, (see figure 3.7) consisting of only four noded elements was created using the same nodes that formed the heat transfer analysis mesh.

This allows for the transfer of nodal temperature data from the thermal analysis to the stress analysis even though the two meshes are not identical. The mesh includes one-half of the plate section as well as the rigid welding table.

Because the material response during welding approaches incompressibility at high temperatures, the hydrostatic stress is expected to be very sensitive to the displacement solution. That is, a very small change in displacement produces large changes in the hydrostatic stress and hence the state variables. To avoid this singularity problem hybrid elements which treat the hydrostatic stress as an independently interpolated state variable have been used. In this hybrid formulation a constant hydrostatic stress is assumed in a 4 noded iso-parametric element (ABAQUS-CGPE6H).

The welding table is modelled by a rigid block represented by two 4 noded isoparametric plane strain elements, (ABAQUS-CPE4) at the end of the plate section. The purpose of this block is to enforce the kinematic condition that no downward rotation should occur during welding. The frictionless contact between the welding table and the rigid block is modelled by two special interface elements (ABAQUS-INTER2).

3.4 MATERIAL MODELLING

The calculation of welding residual stress requires a knowledge of the mechanical and physical properties of the parent plate as well as the weld metal. Using the kinematic hardening model the mechanical properties required are the Elastic Modulus (E), Poisson's Ratio (ν), the virgin Yield Stress (σ_y) and the Tangent Modulus (E_t). The physical property required is the average Coefficient of Thermal Expansion (Ω).

For the submerged arc weld under investigation the parent plate material is AISI 316L stainless steel which has the typical composition listed in Table 2.3. The weld metal deposited is ER 316L stainless steel which has a slightly lower carbon and silicon content and 0.14% Niobium compared to 316L. Because of the small difference in composition between 316L and ER316L, and a 46% dilution of the weld metal during welding, it has been assumed that the weld metal has the same mechanical and physical properties as the parent plate.

A survey of the literature revealed that the material properties of 316L stainless steel are not readily available whereas the properties of 316 stainless steel are. The two steels are of almost identical composition except for the 0.08% carbon in 316 compared to 0.03% in 316L. Because of the slight difference in composition the mechanical and physical properties of the two steels are expected to be similar. Thus in the absence of the 316L material data, the data of 316 stainless steel will be used in the present study.

During welding the plate material is subjected to temperatures from room temperature up to above melting. The properties required for the stress calculations vary considerably over this wide range of temperatures. Unfortunately temperature dependent properties, particularly those at the higher temperatures encountered during welding, are seldom available in the literature. Thus extrapolation from the available low temperature data to higher temperatures is necessary.

Above the melting point the material becomes molten and has very little strength, the extrapolated material properties at melt must be able to simulate this condition without creating numerical difficulties in the solution algorithm [3.6]. In the following sections the

temperature dependent mechanical and physical properties adopted for the stress analysis will be presented. The assumptions made in extrapolating available low temperature data to above the melting point will be discussed.

3.4.1 Elastic Modulus (E)

The Elastic Modulus of a 316L stainless steel was found in reference [3.7] for temperatures up to 816°C. The value of Modulus at room temperature is approximately 200 GPa and decreases linearly to 132 GPa at 816°C. Above this temperature no Modulus data is available, thus extrapolation from the existing data to above the melting temperature (1375°C) was necessary.

In a study of residual stresses in girth butt welds, Rybicki [3.8] has assumed that the elastic modulus of a 304 stainless steel drops linearly from its value at 800°C to almost zero at 1100°C, based on the assumption that the material loses most of its elasticity above 1100°C. However Wray [3.9] in a review of mechanical properties of steels for the modelling of solidification processes has found that austenitic iron retains almost one-quarter of its room temperature modulus value at the melting temperature. It is believed that the Elastic Modulus of a 316L stainless steel behaves in a similar manner. Based on this assumption the Elastic Modulus of 316L should drop from 132 GPa at 816°C to approximately 50 GPa at melt.

An initial study indicated that the Elastic Modulus adopted for temperatures above 816°C will have little effect on the calculated residual stresses. This is because at these high temperatures the material is likely to deform in the elastic-plastic range where the tangent modulus is relatively small so that the elastic modulus has

little or no influence on the calculated stresses. The convergence of the equilibrium solution however is very sensitive to the choice of Elastic Modulus at these high temperatures. This is because during the first iteration of Newton's equilibrium algorithm the incremental stresses are evaluated based on a fully elastic assumption which results in severe overestimation of the incremental stresses and hence the residual forces during the first iteration of each increment if the Elastic Modulus adopted is large compared to the Tangent Modulus.

In mild cases the convergence of the equilibrium solution requires more iterations and smaller time increments. For severe cases, the residual forces oscillate from iteration to iteration and fail to converge even when very small time increments are used. A preliminary study using the assumption that the Elastic Modulus decreases from its 816°C value to 50 GPa at temperatures above melting proved to be one of the severe cases. It failed to converge even when a time increment of 10^{-5} sec was used during the period when the fusion zone is molten.

In the present study the Elastic Modulus above 816°C was assumed to vary from 132 GPa at 816°C to 20 GPa at the melting point (see figure 3.8). Though this material assumption does not represent the real elastic material behaviour at the higher temperatures it allows fast solution convergence with negligible error.

3.4.2 Poisson's Ratio (ν)

The Poisson's ratio of 316 stainless steel and its British counterpart EN58J was found in [3.7] and [3.10] for temperatures up to 816°C. This data showed that the Poisson's ratio varied non-linearly as a function of temperature between values of 0.24 and 0.34 (see figure 3.9).

No data is available above the melting temperature but various researchers [3.6,3.11] have suggested that Poisson's ratio should rise to 0.5 above the melting temperature to simulate an incompressible condition. However this is not necessary because above the melting temperature the material is assumed to behave in a fully plastic manner (zero tangent modulus) so that the incompressible condition is satisfied. It was also found in a preliminary study that above the melting point the Poisson's Ratio adopted had little or no influence on the calculated stresses. In addition specifying a Poisson's ratio approaching the value of 0.5 is computationally unattractive because during the first iteration of each increment of the solution the material is assumed to behave elastically, so that specifying Poisson's ratio close to 0.5 will result in large initial material stiffness matrix, causing the solution to diverge or at best converge very slowly.

In an initial study two extrapolations of Poisson's ratio from 816°C to the melting temperature were examined. One assumed Poisson's ratio to rise from 0.24 to 0.45 at melting, while the other assumed a constant value of 0.24 above 816°C. The results from the two analyses are almost identical but the computing time required for the first assumption is 50% higher than the later one. Thus in this study the Poisson's ratio is assumed to have a constant value of 0.24 above the melting temperature.

3.4.3 Virgin Yield Stress (σ_y)

The simple kinematic hardening theory adopted for this stress analysis requires the stress-strain relationship be linearized into two segments; an elastic segment having a slope equal to the Elastic Modulus

and an elastic-plastic segment which best approximates the work hardening of the material. The intersection point of these two lines is the virgin yield stress. In the present study the 0.2% offset yield stress from [3.12] has been used. The temperature variation of σ_y up to 816°C is shown in figure 3.10. The yield strength at room temperature is approximately 233 MPa and drops almost linearly to 75 MPa at 816°C. Above this temperature it is reasonable to assume that σ_y decreases linearly to zero at melting. However specifying a zero yield stress will result in a zero yield surface and hence a division by zero in the analysis. Also too small a value of σ_y in the molten metal will make part of the model excessively flexible resulting in convergence problems in the equilibrium solution. In this study σ_y is assumed to have a value of 1 MPa above the melting temperature which is approximately 0.5% of its room temperature value.

3.4.4 Tangent Modulus (E_t)

The Tangent Modulus is the slope of the elastic-plastic segment of a bilinear stress-strain curve. Unfortunately only the room temperature stress-strain curve of 316L stainless steel is available. As a result the Tangent Modulus at other temperatures must be approximated. At room temperature the Tangent Modulus was estimated to be 2800 MPa. The Tangent Modulus at other temperatures was calculated based on the assumption that the E_t/E ratio remains constant up to the melting temperature [3.6]. Figure 3.11 shows the resulting Tangent Modulus variation with temperature.

Above the melting temperature the Tangent Modulus has been set to zero so that the material behaves in a fully plastic manner in the molten state. In addition a zero Tangent Modulus above melting implies

that the molten material will resolidify as a damage-free material. This is because the kinematic shift, α_s , which is the state variable used in the kinematic hardening theory to record the plastic deformation history of the material, is reset to zero if a zero Tangent Modulus is specified.

$$\sigma_s = E_t \left[\Delta \epsilon^{-P} \frac{\sigma}{\sigma_y} + \frac{\alpha_s^0}{E_t} \right] \quad (3.4)$$

where $\Delta \epsilon^{-P}$ = incremental equivalent plastic strain

σ_y = virgin Yield Stress

$$\frac{\sigma}{\sigma_y} = \frac{\sigma}{\sigma_y} - \frac{\alpha_s}{\sigma_y}$$

σ = stress tensor

α_s, α_s^0 = kinematic shift of the current and last increment

E_t, E_t^0 = Tangent Modulus of the current and last increment

In the computations a division by zero problem will occur in subsequent increments when the tangent modulus E_t^0 becomes zero. However because α_s^0 will also be zero, this problem can be solved by setting the α_s^0/E_t^0 term to zero in the above equation when E_t^0 becomes zero.

3.4.5 Average Coefficient of Thermal Linear Expansion (Ω)

The percent thermal expansion (γ) data of 316 stainless steel was found in reference [3.13] for temperatures up to the melting point. However ABAQUS requires the average Coefficient of Thermal Expansion as the input data. With this input the thermal strain at a material point at the temperature T can be calculated as,

$$\epsilon^{th} = \Omega(T)(T - T_0) - \Omega(T_I)(T_I - T_0) \quad (3.5)$$

where T_I = initial stress free temperature (20°C)

T_o = reference temperature for the Coefficient of Thermal Expansion data (20°C)

The expansion coefficient Ω can be obtained from γ as,

$$\Omega (T) = \frac{\gamma (T)}{100 (T - 20^{\circ}\text{C})} \quad 1/^{\circ}\text{C} \quad (3.6)$$

The above equation fails when the temperature T is equal to 20°C , but based on physical reasoning $\Omega (20^{\circ}\text{C})$ can be approximated by extrapolation from the higher temperatures. The resulting temperature variation of Ω is shown in Figure 3.12.

3.5 RESULTS OF ANALYSIS

Using the material model previously described and the temperature history of Chapter 2 the stress history of the single pass weld described in Appendix B has been calculated. The results of the analysis are described in the following sections.

3.5.1 Distortion

Figure 3.13 shows the angular rotation of the plate section after welding. During the initial heating period the plate is in contact with the welding table which prevents any downward rotation. Upon cooling the plate separates from the welding table and rotates upward. The final separation between the end of the plate and the welding table is 0.68 cm. This would give a predicted separation of 1.36 cm if a full plate were considered and held on one side as in the experiment. In the actual experiment the measured separation after welding was 1.19 cm (from Appendix B) which is 12% lower than the predicted separation.

A plot of the displaced mesh in the fusion zone after complete cooling is shown in Figure 3.14. No severe distortion of the finite element mesh is observed, which indicates that the residual force tolerance chosen is sufficiently small to ensure convergence of nodal displacements.

3.5.2 Residual Stresses

3.5.2.1 Longitudinal Stress

The distribution of longitudinal stress (σ_x) on the top surface of the plate at different times is plotted in Figure 3.15. Upon initial heating ($t = 1.45$ seconds) the longitudinal stress in the fusion zone is compressive but of negligible magnitude because of the low yield strength of the material above the melting temperature. A larger compressive stress is developed immediately adjacent to the weld pool where the material has retained some strength. Upon solidification of the weld metal, unloading and reversed yielding occur quickly due to the low material strength at high temperature. Upon cooling a tensile stress is developed in the fusion zone and the adjacent area. This tensile zone continues to grow as the weld cools. The final residual longitudinal stress distribution shows that a peak value of about 300 MPa occurs just inside the fusion zone. Outside the fusion region the longitudinal stress drops to 230 MPa. This is probably due to the Bauschinger effect caused by severe compressive yielding of the material in this region during the initial heatup period. A nearly constant stress level of 230 MPa is observed outside the fusion zone up to 2.5 cm away from the weld centreline. Beyond this point the longitudinal stress drops almost linearly until it becomes compression about 4 cm

from the weld centreline. The maximum compressive stress is about 30 MPa and extends uniformly towards the edge of the plate. This compressive stress is developed because the large contraction in the weld zone pulls the two rigid planes bounding the plane of analysis closer together thus putting a compressive load on the rest of the plate section.

The distribution of longitudinal stress on the bottom surface of the plate at different times is plotted in Figure 3.16. Although no melting occurs on the bottom surface, the high temperature and low strength of the material is sufficient to cause compressive yielding during the initial heating period. Upon cooling the region close to the weld line develops a tensile stress zone. The final residual longitudinal stress distribution is almost identical to that on the top surface except that the stresses close to the weld line are lower in magnitude.

The distribution of longitudinal stress through the thickness of the plate at the weld centreline is plotted in Figure 3.17. During initial heating, very low stresses occur in the weld pool, while a large compressive stress is developed immediately below it. The final stress distribution shows that the longitudinal residual stress distribution at the weld centreline is all tensile. A peak value of 360 MPa is observed just inside the fusion zone. Below the fusion zone the stress drops to 230 MPa and is almost uniform to the bottom surface of the plate.

A contour plot of the longitudinal residual stress is shown in Figure 3.18. A maximum stress exceeding 340 MPa is observed in the bottom of the fusion zone. Outside the fusion zone a low tensile stress region is observed. This region is subjected to the highest compressive yielding

when the weld pool is molten, which results in the low tensile stresses upon cooling due to the Bauschinger effect.

3.5.2.2 Transverse Stress

Figure 3.19 shows the distribution of transverse stress, σ_x , on the top surface at various times after the passing of the weld arc. During initial heating ($t = 1.45$ seconds) the transverse expansion on the top surface is suppressed because the plate is not allowed to rotate downwards. This results in a compressive transverse stress except in the weld pool where the material has little strength. Upon cooling a compressive zone develops inside the fusion region but turns tensile just outside the weld metal. The compressive stress inside the fusion zone is likely caused by the initial contraction of the weld metal below the surface which forces the plate to rotate upwards thus putting a compressive load on the surface layers. The final residual transverse stress is compressive in the fusion zone while a tensile stress reaching 50 MPa is developed 2 cm from the weld line.

The transverse stress distribution on the bottom surface during welding is shown in Figure 3.20. During initial heating large tensile stresses are developed near the weld centreline ($t = 1.45$ seconds). After complete cooling tensile residual stress is observed between 1 and 3 cm from the weld centreline with a peak value of 50 MPa at the 2 cm location. Negligible transverse stresses are observed elsewhere on the bottom surface.

Figure 3.21 shows the distribution of transverse stress through the thickness at the weld centreline. When the weld pool is molten the stresses below the weld pool correspond to a bending stress caused by the moment created by the reaction forces from the welding table. Upon

cooling the bottom of the weld pool contracts, first producing tensile stresses and causing the plate to rotate upwards, thus compressing the upper layers. A maximum tensile stress of approximately 90 MPa is developed at the bottom of the weld pool upon complete cooling. From this maximum the transverse stress declines almost linearly to a compressive stress of about 50 MPa on the top surface. A compressive region is developed just below the weld pool with stresses up to 70 MPa. This gradually drops to almost zero towards the bottom surface. This transverse stress distribution through the thickness resembles the residual stress distribution resulting from the formation of a plastic hinge.

A contour plot of the transverse residual stress distribution is shown in Figure 3.22. The peak transverse stress occurs near the weld centreline, just inside the fusion region.

3.5.2.3 Through Thickness Direction Stress

The stress magnitude in the through thickness direction, σ_y , is relatively low compared to the transverse and longitudinal stresses. The maximum residual stress observed is approximately 30 MPa (see figures 3.23-3.25). Oscillation of the stresses along the top and bottom surfaces is also observed.

3.6 COMPARISON WITH EXPERIMENTS

Experimental measurements of residual stresses of the welded plates described in Appendix B were undertaken by Dr. G. Roy of EMR, using the cut-and-section method and the hole drilling technique, and Mr. Rick Holt also of EMR, using X-Ray diffraction techniques. Unfortunately final reports of their findings are not available at the time of writing.

However some preliminary results have been released and they will be used here for comparison with numerically predicted values.

3.6.1 Cut-and-Section Method and Hole Drilling Technique Results

In figure 3.26 to 3.29 the experimentally measured residual stresses have been compared to the numerical predictions. On the top surface (Figure 3.26) the measured longitudinal stress of over 400 MPa along the weld centreline seems high compared to the numerically predicted values of 300 MPa. Furthermore the measured stresses indicated the longitudinal stresses tensile zone is over 6 cm wide compared to the predicted zone size of 4.5 cm. The longitudinal stress distribution through the thickness is shown in figure 3.27. The measured stresses are higher near the top surface but lower towards the bottom surface.

The measured transverse stresses along the weld centreline are plotted in figure 3.28. Good agreement between experimental and numerical results can be observed near the top surface. Below the fusion zone the measured transverse stresses are compressive but of a much larger magnitude (140 MPa) than the predicted values (75 MPa) and the peak stress occurs further below the fusion zone.

In general the trends agree for both longitudinal and transverse stresses. While there is disagreement in some peak stresses the results are encouraging. It must be remembered that the experimental results are preliminary and some changes are expected as the experimental techniques are refined.

3.6.2 X-Ray Technique

Residual stress measurements of the experimental welds was also undertaken by Dr. R. Holt of EMR using the X-Ray diffraction technique.

Longitudinal stresses on the top surface of the plate on both sides of the weld centreline were measured and the results are shown in figure 3.29. Within the scatter of the experimental results very good agreement is shown with the predicted values. This is very encouraging and suggests that the welding simulation is accurate.

3.7 EFFECTS OF RESTRAINT CONDITIONS ON THE WELD RESIDUAL STRESSES

The restraint condition of a flat plate during welding will have an important effect on the final residual stress after welding. In the analysis of the experimental weld described in Appendix B the plate was clamped on one side only allowing free rotation of the plate. In the following section other boundary restraint conditions of the same plate welded under identical welding parameters are considered. Since the temperature and the stress analyses are assumed to be uncoupled, the temperature results described in Chapter 2 can be used as input to the residual stress calculation for the various plate restraint conditions.

Three different restraint conditions were examined with either zero plane strain elements or generalized plane strain elements depending on the out-of-plane restraint conditions. The same finite element mesh shown in figure 3.7 was used to examine residual stresses under different restraint conditions and to compare residual stresses for the zero plane strain and generalized plane strain assumption.

3.7.1 Simply Supported Restraint

The first restraint condition examined was a plate simply supported on knife edges on either side of the weld as shown in figure 3.30. With this welding setup any in-plane or out-of-plane deformation is possible.

The in-plane kinematic constraints can be symbolically represented as shown in figure 3.31. The out-of-plane deformations can be modelled in a 2D analysis using generalized plane strain elements with all three degrees of freedom unrestrained ($\Delta u_z, \phi_x, \phi_y = 0.0$).

To determine the effect of bowing and uneven longitudinal shrinkage on residual stresses, two additional generalized plane strain analysis were carried out, one assuming $\phi_y = 0.0$ (no uneven longitudinal shrinkage) and another assuming $\phi_y = \phi_x = 0.0$ (no uneven longitudinal shrinkage and no bowing).

A fourth analysis of the same simply supported plate was conducted using a zero plane strain assumption. Although this assumption is inappropriate for the modelling of an unrestrained plate, it has been adopted by other researchers for the modelling of welding of flat plates [3.4,3.6].

The results of the above analyses are plotted on figures 3.32 to 3.37. The predicted longitudinal stresses are almost identical for all four analysis up to 2 cm from the weld centreline. Beyond 2 cm the size of the tensile stress zone increases as the out-of-plane unrestraint increases, with a maximum tensile zone 5 cm wide occurring for the zero plane strain condition. The largest compressive longitudinal residual stress of 100 MPa is observed for the generalized plane strain assumption with all degrees of freedom unrestrained. With the assumption of no uneven longitudinal shrinkage ($\phi_x = \phi_y = 0.0$), the compressive stresses become much smaller (25 MPa) and are uniformly distributed 4.5 cm beyond the weld centreline. The zero plane strain assumption gives no compressive zone at all. This is expected because in a zero plane strain assumption, the displacements between the two

parallel planes bounding the section of analysis are zero, so that only regions subjected to compressive yielding during the heating period will develop tensile longitudinal residual stress.

The variation of the transverse stress distribution (shown in figures 3.35 to 3.37) under the various restraint conditions is minimal.

A summary of the deformations after welding of a simply supported flat plate under the different out-of-plane restraint assumptions is given in Table 3.1. The results indicate that the in-plane angular rotation, as shown by the downward displacement Δu_y at the bottom of the weld line is not sensitive to the out-of-plane restraint assumption adopted. Comparing Δu_z of case 2 and 3 shows that the inclusion of bowing has very little effect on the contraction between the bounding planes. However allowing an uneven longitudinal shrinkage (ϕ_y) in the model results in approximately 400% increase in the contraction along the weld centreline. This could explain why the compressive longitudinal stresses shown in figures 3.32 and 3.33 are four times higher than those results with ϕ_y restrained. The $\phi_y = 0.0$ assumption is probably realistic when the plate section being analysed is located at the mid-length of a weld. In this case rotation of the bounding planes about the y axis would be prevented or at least hindered by the surrounding material.

3.7.2 Plate Clamped Along Both Edges

In this example the welded plate is supported on I beams and clamped along both edges as shown in figure 3.38. In-plane rotation of the plate section is limited by the clamping but the plate is free to slide in the transverse direction. This in-plane restraint condition

can be symbolically represented in figure 3.39. A generalized plane strain condition has been assumed with the rotation about the x and y axes restrained ($\phi_x = 0.0$ due to the clamping). This assumption allows longitudinal shrinkage but no bowing.

The results of the analysis compared to those of a simply supported plate are shown in figures 3.40 to 3.45. With no bowing or angular rotation of the plate, the predicted longitudinal stress pattern are very similar to those of the simply supported plate, except with a decrease of approximately 30 MPa of the longitudinal stress on the top surface near the weld centreline and an increase of equal magnitude on the bottom surface. Approximately 2 cm beyond the weld centreline, the longitudinal stresses are almost identical.

The transverse stress pattern under the clamped condition is very similar to that of the simply supported plate. However because of the clamping, the transverse stresses on the top surface becomes more tensile resulting in a general increase of the stresses and a maximum tensile stress of 120 MPa instead of 75 MPa. It should also be pointed out that the transverse stress in the fusion zone is no longer compressive. On the bottom surface, the transverse stresses decrease resulting in compressive stresses up to 50 MPa. The through thickness transverse stress distribution resembles a plate under bending with tensile stress near the top and compressive stress near the bottom surfaces.

3.7.3 Fully Restrained

In this analysis the welded plate was assumed to be fixed around its edges (figure 3.46) preventing longitudinal and transverse shrinkage. This restraint condition on the plate simulates the welding in the middle of an infinite plate. Symbolically the in-plane restraint

conditions are shown in figure 3.47. No out-of-plane deformations are allowed thus zero plane strain elements can be used in this analysis.

The results of this analysis are shown in figures 3.48 to 3.53. Because of the additional external restraints, the residual stresses in both the longitudinal and transverse directions were significantly different than a plate only clamped along two edges. The residual longitudinal stress on the top surface inside the fusion zone is increased by 10% from 320 MPa to 350 MPa. Because of the zero plane strain assumption the size of the longitudinal tensile stress zone is increased to 6 cm from the weld line. The peak longitudinal stress on the bottom surface is increased from 200 MPa to almost 250 MPa.

With clamping along the edges but free transverse shrinkage, the transverse stress distribution through the thickness resembles a plate section under bending with tensile stress near the top and compressive stress near the bottom. However when the plate ends are rigidly held, as in the present case, the transverse shrinkage is prevented, resulting in an additional uniform shrinkage stress through the thickness. The resulting transverse stress distribution is equivalent to the superposition of a uniform tensile stress of 100 MPa and the residual stresses existing in a plate which is only clamped along the edges.

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Chapter IV

PREDICTION OF RESIDUAL STRESSES IN MULTI-PASS WELDS: -TWO PASS WELDS

4.0 INTRODUCTION

Fabrication of the majority of modern structures requires multi-pass welding to join heavy sections. It is important to know the residual stress distribution of such weldments in view of the fact that fatigue or fracture failure of such structures is often catastrophic. As described in Appendix A many researchers have simplified the analysis of multi-pass welds by grouping passes into layers with a single analysis for each layer or even grouping layers to reduce the number of analyses. Unfortunately there is little proof of the accuracy of these simplifications. The general objective of this study is to examine the behaviour of simple multi-pass welds to determine the feasibility of these simplifying procedures. As mentioned in previous chapters it is believed that a single pass weld can be simulated accurately, therefore to achieve our objective, a study was conducted with complete simulation of all weld passes in a multi-pass weldment. From these results information on how residual stresses redistribute after each weld pass was determined and the feasibility of grouping weld passes or layers into a simplified analysis was studied.

Because of the high computation cost involved in the analysis of multi-pass welds only simple weldments consisting of two and three passes were examined. The two pass results are described in this chapter while

the three pass weld results are described in chapter 5. A discussion on the simplification of multi-pass weld analysis is presented in Chapter 6.

4.1 TWO PASS WELDS

In the study of two pass welds it was decided to examine two weld configurations; one with two passes laid vertically on top of each other in a V weld preparation (figure 4.1) and the second with two passes laid side by side in a weld preparation (figure 4.2). The plate dimensions and material composition are assumed to be identical to the plates used in the single pass experiments (Appendix B). The result of the first weld configuration should provide information on the interaction of stresses between layers of weld passes and the result of the second configuration information on how stresses of weld passes on the same layer interact. It must be pointed out that the weld preparations described above are somewhat idealized to provide a comparison with the single pass weld. Nevertheless, the analysis results should provide information regarding interaction and redistribution of residual stresses in practical multi-pass welds. It was also assumed that the heat input to each weld pass was the same. In practice this would require that the welding speed be adjusted to achieve the weld pool size assumed for each weld pass.

The finite element heat transfer and stress analysis models developed for the single pass weld, described in chapters 2 and 3, were adopted here for the simulation of each weld pass. Experimental verification of the multi-pass analysis is currently under study at Energy, Mines and Resources, Ottawa. However, it is believed that the single pass models can be applied here without error and can be used to draw conclusions regarding weld interaction. Because of symmetry about the

centerline of the two weld passes laid vertically only half of the plate section need be analysed. No symmetry exists in the weld passes laid side by side and the entire plate cross-section is included in the model. These plate sections were discretized into four noded isoparametric finite elements as shown in figures 4.3 and 4.4. These meshes were used in both the heat transfer and the stress analyses.

4.2 HEAT TRANSFER ANALYSIS

In the heat transfer analysis, each weld pass was assumed to have the same welding parameters and thermal boundary conditions as the single pass weld described in chapter 2. Complete cooling to room temperature between passes was also assumed. The filler material of each weld pass was modelled as being deposited instantaneously just before the welding begins. During welding of the first pass, the second pass weld metal is removed from the model using the MODEL CHANGE option in ABAQUS. At the end of the first pass the material of the second pass is introduced into the model for the second pass analysis.

During welding it was assumed that the weld preparation region of the two welds was covered by a granulated flux blanket of 5.6 cm width and therefore this portion of the weldment surface was assumed to be thermally insulated. The resulting thermal boundary conditions are shown in figures 4.5 and 4.6 respectively.

Goldak's heat source model used in the single pass analysis was used to model the heat input from each weld pass. This model was modified slightly to obtain the slanted weld bead shapes shown in figure 4.8 for the two weld passes laid side by side. For the two passes laid vertically only the depth of the heat source required relocation (figure 4.7).

4.3 STRESS ANALYSIS

During welding the weldments were clamped along both edges so that in a two dimensional generalized plane strain model the only out-of-plane distortion permitted is the uniform displacement between the planes bounding the section of analysis. In-plane displacements were permitted but rotation of the edges of the plate as in the single pass weld was not allowed due to clamping. A symbolic representations of the in-plane boundary constraints for the two pass welds are shown in figures 4.9 and 4.10.

In a similar manner as the heat transfer analysis, the second pass material in the stress analysis was removed from the model during the welding of the first pass. However for the second pass introducing the undeformed filler material into the deformed weld preparation using the MODEL CHANGE option of ABAQUS created problems. Ideally the filler material shape as specified by the interface node coordinates should be modified to reflect the change in weld preparation geometry resulting from the accumulated strain in the first weld pass. Instead the MODEL CHANGE option attempts to stretch the undeformed material to fit it into the deformed geometry. This results in a stress built up in the supposedly stress free filler material and a redistribution of the residual stresses in the first weld pass. An attempt to correct this problem by initially considering the filler material to have the properties of a perfectly plastic material with a very low yield stress, similar to that used at the melting temperature but with a zero thermal expansion coefficient, was unsuccessful when the solution failed to converge. This is probably due to the large displacements of the deformed weld geometry and the low stiffness of the material specified.

To overcome this difficulty the displacements of the weld preparation interface nodes after the first pass were applied to the filler material as a boundary condition before the filler material was introduced. The filler material was considered as having a very low Modulus and no Coefficient of Thermal Expansion so that this applied distortion creates negligible stress. After the filler material has been inserted into the weld preparation intermediate steps were used to remove these boundary constraints from the model and return the material properties to normal. Unfortunately, this technique necessitates that the analysis be stopped after each weld pass so that the weld preparation interface displacements can be retrieved to specify the boundary constraints to be applied to the filler material of the next weld pass.

4.4 ANALYSIS RESULTS

The input data for the heat transfer and thermal stress analyses for the two weld passes laid vertically and the two weld passes laid side by side are listed in Appendix F and Appendix G respectively. In the following sections the results of these analyses are presented.

4.4.1 Two Passes Laid Vertically

4.4.1.1 Temperature

The temperature contours around the fusion zone when the weld pool has reached its maximum size are shown in figure 4.11 for the first pass and figure 4.12 for the second pass. The contours around the two weld beads are very smooth although the finite element mesh does not conform to the bead shape as in the single pass analysis. This leads to the conclusion that the mesh does not have to conform to the weld bead shape in order to obtain good results at the solid/liquid interface, and

therefore mesh generation in a multi-pass weld configuration can be simplified considerably.

4.4.1.2 Distortion

The deformed local weld geometry after welding is shown in figure 4.13. Distortion of the elements in the weld pool region is not severe which suggests good convergence of the displacement solution has been achieved. Figure 4.14 shows the predicted in-plane distortion of the whole plate section after the completion of the two pass weld. The downward displacement of the plate at the weld centreline, caused by the uneven shrinkage of the upper and lower surface, is about 0.17 cm.

4.4.1.3 Residual Stresses

Contours plots of the longitudinal and transverse residual stresses after the welding of each pass are shown in figures 4.15 to 4.18. Stress distributions on the top and bottom surfaces as well as along the weld centreline are shown in figures 4.19 to 4.24. Through thickness direction stresses are of smaller magnitude (max. 40 MPa) and therefore have not been presented.

Figure 4.15 shows the longitudinal stress contours after the first weld pass. The stress distribution around the weld pool is very similar to that of a single pass weld with a maximum of 380 MPa located at the bottom of the fusion zone. After the welding of the second pass, only small changes of the contours around the first pass fusion region are observed (see figure 4.16). This indicates that changes of the first pass residual stresses due to the second weld pass are small. The decreasing contours surrounding the first pass fusion zone remain after the second pass. The second pass appears to simply add to the size of

the high tensile stress region of the weld metal without much change in the stress gradient in the parent material.

Figures 4.17 to 4.18 show the transverse stress contours after the welding of the first and second pass respectively. In both cases tensile stresses are observed on the top surface and compressive stresses on the bottom surface, indicating a bending effect. The stresses near the top surface increased over 100% after the completion of the second pass. This is believed to be due to the transverse shrinkage of the second weld pass and an increase in restraint with the inclusion of the second pass filler material. In general the transverse stress are less than 25% of the longitudinal stresses.

Figures 4.19 to 4.20 show that the longitudinal residual stresses on the top and bottom surfaces after the welding of each pass are almost identical to that of the single pass results described in Section 3.7.2. Along the weld centreline (figure 4.21) the difference between the longitudinal stress distribution of the two pass weld and the single pass weld is that the high tensile stress zone (exceeding 300 MPa) of the two pass weld extends from the top surface to the bottom of the fusion zone of the first weld pass. Relaxation of the first pass residual stresses due to the second weld pass is small (less than 30 MPa). This reduction in longitudinal stress is believed to be due to the shrinkage of the second pass material imposing a compressive stress onto existing stresses.

Figures 4.22 and 4.23 show the transverse residual stresses on the top and bottom surfaces. After the completion of the first weld pass the compressive transverse stresses on the bottom surface are similar to those of the single pass weld, while lower tensile stresses

are observed on the top surface particularly close to the weld preparation region. With the deposition of the second pass the stress magnitudes almost double on both the top and bottom surfaces. The distribution of transverse stress through the thickness along the weld centreline is shown in figure 4.24. The stress distribution after the completion of the first pass resembles that of a single pass weld, with a tensile zone inside the fusion region and a compressive zone below it. After the welding of the second pass the residual stresses developed by the first weld pass are modified by a compressive stress resulting from the shrinkage effect of the second weld bead.

4.4.2 Two Passes Laid Side by Side

4.4.2.1 Temperature

The maximum fusion zone size of each weld pass is shown in figures 4.25 and 4.26 for the first and second passes respectively. The first weld bead is slanted towards the weld preparation. As expected both weld beads are very similar in size compared to the single pass weld.

4.4.2.2 Distortion

The deformed plate geometry is shown in figures 4.27 and 4.28. Figure 4.27 shows that the distortion of the finite elements in the fusion region is not severe which indicates good convergence of the displacement solution. Figure 4.28 shows the in-plane displacement after welding. A downward displacement of 0.19 cm has been predicted along the weld centreline.

4.4.2.3 Residual Stresses

Contour plots of the longitudinal and transverse residual stresses after the welding of each pass are shown in figures 4.29 to 4.32.

Stresses on the top and bottom surfaces are plotted in figures 4.33 to 4.36.

The longitudinal residual stress contour plot after the first weld pass (figure 4.29) is very similar to that of the single pass weld. A maximum residual stress of 390 MPa occurs at the bottom of the fusion zone. After the welding of the second pass similar decreasing contours exist but move to surround the fusion region of both weld passes. A peak stress of 400 MPa is observed at the bottom of the second weld bead. Although the stress contour pattern surrounding the first pass material before and after the welding of the second pass are similar, the stress magnitude have been increased by approximately 20 MPa. Also the stress contours after the second weld pass are not symmetrical about the plate centreline, possibly because of the slanted bead shape of the first weld pass.

The transverse residual stress contour plot after the welding of the first pass is shown in figure 4.31. A bending effect is observed with tensile stresses near the top surface and compressive stresses near the bottom surface. The maximum stress of 100 MPa occurs in the weld preparation region adjacent to the first weld bead. After the welding of the second pass, the maximum stress shifted upwards to just inside the second pass fusion zone. Again the familiar bending affect is observed through the thickness of the plate.

Figures 4.33 and 4.34 show the longitudinal stresses on the top and bottom surfaces after each weld pass. It can be observed that after the welding of the second pass, the first pass longitudinal residual stresses are increased by approximately 20 MPa on the top surface while decreasing by the same amount on the bottom surface. The final

longitudinal stress distribution on the top and bottom surfaces are reasonably symmetrical about the weld centreline.

Figures 4.35 and 4.36 confirm that the weld passes produce tensile transverse residual stresses on the top surface and compressive residual stress on the bottom surface. However the stress magnitudes after the second weld pass are almost doubled that after the first weld pass. Note also a lower stress region on the top surface in the fusion area of the zone.

Chapter V

PREDICTION OF RESIDUAL STRESSES IN MULTI-PASS WELDS: -THREE PASS WELD

5.0 INTRODUCTION

The study of residual stresses in two pass weldments has been described in Chapter 4. In this chapter the results of a study of a three pass weldment are presented.

5.1 THREE PASS WELDS

For this study of a three pass weldment it was decided to examine a weld with three passes laid side by side in a weld preparation on the top surface of a flat plate (see figure 5.1). The results of this analysis should provide information on the interaction of stresses in a layer of three or more weld passes. The plate dimensions and material composition are assumed to be identical to the plate used in the single pass experiments (Appendix B). The technique of handling the filler material of each weld pass is the same as that used in the two pass weld analyses and therefore will not be described. Because there is no symmetry about the centreline the entire plate cross-section must be included in the finite element model. This plate section is discretized into four noded isoparametric elements as shown in figure 5.2. This mesh is used for both the heat transfer and the stress analyses.

5.2 HEAT TRANSFER ANALYSIS

During welding it was assumed that the weld preparation region was covered by a granulated flux blanket 6 cm wide and therefore this region

of the weldment was assumed to be thermally insulated while free convection and radiation took place elsewhere on the plate surface. The resulting thermal boundary conditions are shown on figure 5.3.

The heat input distribution of each weld pass is modelled using Goldak's ellipsoidal heat source model. The first two passes required a slight modification of this model to achieve the slanted weld bead shapes shown in figure 5.4.

5.3 STRESS ANALYSIS

During welding the plate was clamped along both edges, the same as the two pass welds described in chapter 4, and therefore the same in-plane and out-of-plane distortion assumptions were adopted.

5.4 ANALYSIS RESULTS

5.4.1 Temperature

The predicted weld pool shape of each weld pass is shown in figures 5.5 to 5.7. The pool sizes are very similar to that of a single pass weld, however the positioning of the centre of the heat source for the first and second weld pass are slightly in error, resulting in incomplete melting of the filler material at the right hand corner of the weld pool in the bottom of the weld preparation. However this is not expected to have a significant effect on the calculated residual stresses.

5.4.2 Distortion

Figure 5.8 shows the distorted finite element mesh in the fusion region after the completion of welding. No severe distortion of the elements are observed indicating good convergence of the displacement solution. The deformed plate section is shown in figure 5.9 indicating a downward placement at the plate centreline of approximately 0.14 cm.

5.4.3 Residual Stresses

The contour plot of longitudinal stresses after the welding of each pass are shown in figures 5.10 to 5.12. The high tensile stress region (exceeding 300 MPa) surrounded by the decreasing stress contours expands after the welding of each pass. The stress contours after the completion of the three passes resembles a low penetration single pass weld with a depth to width ratio of 1 to 3.

The contour plots of the transverse stresses after each pass are shown in figures 5.13 to 5.15. A bending stress distribution can be observed in all three plots. The most severe bending stresses occur through the thickness of the plate in the weld preparation region where the plate is the thinnest. After the completion of the last pass, the stresses are almost uniform along the length of the plate section. However a peak transverse stress region, where the stress exceeds 80 MPa, occurs in the fusion region of the last weld pass.

The longitudinal and transverse residual stress distribution on the top and bottom surfaces of the plate after each weld pass are shown in figures 5.16 to 5.19. It can be observed that the longitudinal tensile stress zone increases with the welding of each pass. The peak tensile stress region also increases and widens with each weld pass, eventually extending over the entire fusion zone after completion of welding. The peak stress on the top surface increased by approximately 20 MPa after each pass. The final longitudinal residual stress distribution are almost symmetrical about the plate centreline after the completion of welding. The distribution also resembles the longitudinal stress distribution one would observe in a single pass weld except the tensile stress zone and peak stress region are wider due to the wider fusion region.

The transverse stress distribution on the top and bottom surface are shown in figures 5.18 and 5.19 respectively. It can be seen that the stress distribution are almost identical after the welding of the first two passes. With the welding of the last weld pass, the stresses remain tensile on the top and compressive on the bottom surface but with an increase in magnitude of close to 100%. From the above observation it can be said that the transverse stresses in a multi pass weld are not fully developed until the very last weld bead has been laid.

Chapter VI

DISCUSSION OF RESULTS

6.1 SINGLE PASS WELD

The predicted residual stresses for the single pass weld described in Chapters 2 and 3 appear to agree with the limited experimental results reasonably well. Further experimental confirmation would be very desirable but must wait completion of the test program at Energy, Mines and Resources, Ottawa. If the results of the single pass weld are in good agreement with experiment it is reasonable to assume that the multipass residual stress predictions are accurate since the process of analysis is simply repeated for each weld pass. Therefore the most important aspect of multipass weld analysis is to achieve an accurate representation of a single pass weld.

There are a number of commercially available finite element codes such as ABAQUS which have the non linear analysis capability to model the welding process. The use of such commercial codes should facilitate the convenient transfer of technology from a university/research environment to industry since such codes are readily available.

The code ABAQUS, with a few exceptions to be discussed later, contains most of the necessary models to simulate the various aspects of welding. However if one attempts to model all mechanisms present in the welding process the resulting non linearities in the finite element analysis lead to extremely long and impractical computing requirements. Therefore it is necessary to identify those mechanisms, models and parameters which are essential to producing accurate results.

Conversely, for computing efficiency it is necessary to identify those mechanisms, models and parameters which can be simplified or ignored.

Accurate simulation of a single pass weld (and consequently multi pass weld) requires consideration in four areas:

- a) Heat input and heat loss mechanisms
- b) Elastic-Plastic material behaviour
- c) Material properties
- d) Restraint and support conditions

In each of these areas various parameters or models are required, some of considerable importance others of less importance.

6.1.1 Heat Input and Heat Losses

For submerged arc welding it appears that weld efficiencies reported in the literature are sufficiently accurate for simulation purposes. The distribution of heat input is very important and should be applied to give an accurate weld pool shape. Goldak's double ellipsoid model for the heat input distribution appears to be the most accurate and convenient model to use. It requires an estimate of the total heat input and the distribution of this heat into each of the ellipsoids. The former can be determined from the power input and assumed weld efficiency and the latter from the size and shape of the weld pool estimated or measured on the completed weld. It should be noted however that an accurate prediction of the weld pool shape is not a prerequisite for the accurate prediction of residual stresses.

Heat losses by convection and radiation from the plate away from the weld pool appear to have only a minor effect on the residual stress. Heat losses by radiation and convection in the vicinity of weld bead

were not included in the model as a submerged arc weld was considered. It is expected that these losses would be significant for other welding processes but may be partially compensated for in the value of weld efficiency used. Unfortunately for many welding process the reported scatter on weld efficiency is large. This may be due in part to the heat losses from the weld pool.

Other sources of heating or heat loss such as latent heat and phase transformations occurring below the melting temperature have not been studied but their effect on the temperature history is believed to be small in the welding of 316L stainless steel.

6.1.2 Elastic-Plastic Material Behaviour

In general finite element codes such as ABAQUS can model elastic-plastic behaviour during welding since most contain a Kinematic Hardening model to account for the Bauschinger Effect. However since these codes have not been developed specifically for welding there may be difficulties in three modelling areas:

- a) Numerical stability when the material being considered is at a high temperature and therefore very low stiffness, eg. the weld pool and adjacent regions.
- b) The consideration of the additional material (weld metal) to be placed in the weld preparation.
- c) Modelling of the resolidification of the molten metal as a damage free virgin material.

It is in these areas that the code may not have the modelling features necessary. In such cases an acceptable simulation can often be developed through adoption of unusual material properties, temperature histories or finite element models to overcome deficiencies in the code,

as has been done in this study. Unfortunately the development of such schemes can be very time consuming because they often require extensive testing in the non-linear regime. In addition they tend to be specific in their application and to the code used. Therefore the techniques developed for this report may not be applicable to other codes. Deficiencies in the code often are a result of attempts to provide convenience rather than a fundamental problem. It appear quite feasible that commercial codes can be modified to provide the necessary modelling features for the welding problem. For example, consider the MODEL CHANGE option in the Code ABAQUS. It has been described earlier that the introduction of the additional weld metal material creates a problem using this ABAQUS feature: In a multipass weld additional material (elements) must be added for each weld pass. This can be done in ABAQUS with the MODEL CHANGE option. Unfortunately the program has been written so that elements (weld metal) inserted into a deformed cavity (weld preparation) are forced to conform to the cavity. While appropriate for some classes of problems this feature introduces stress into the elements which is not consistent with the addition of molten, zero stiffness, weld metal. This problem has been overcome by adding the cavity deformation to the element coordinates so a perfect fit is accomplished without inducing additional stresses. That is, the input data can be modified to overcome code deficiencies however it required considerable testing to ensure that the approach was correct.

The current study has considered 316L stainless steel which does not exhibit significant phase transformations. The consideration of Ferritic steels may require the modelling of phase transformations since it is believed that the volumetric changes accompanying phase transformations

alter residual stresses particularly if such transformations occur at lower temperatures.

6.1.3 Material Properties

A major component of input to the finite element analysis are the material parameters as a function of temperature. At higher temperatures these must be estimated or extrapolated from low temperature data as little information exists in the literature.

The material properties required for the temperature history analysis are the Latent Heat of Fusion, Thermal Conductivity, Specific Heat and material Density. Accurate thermal properties below the melting point are necessary for the accurate prediction of the temperature history. Above melting, the complex heat transfer mechanisms are very difficult to model even with accurate thermal properties. Instead the thermal properties above melting are chosen to obtain an accurate prediction of weld pool shape. Below the melting point, all thermal properties required in the heat transfer analysis are temperature dependent. However in an uncoupled thermal stress analysis in which the geometry of the finite elements do not change during the temperature analysis the density can be assumed to be constant (the room temperature value).

The Latent Heats of Fusion of many alloys are not available and therefore must be estimated from the Latent Heat of Fusion of the individual elements making up the alloy.

The material properties required for the stress analysis are the Average Coefficient of Thermal Expansion, Elastic Modulus, Poisson's Ratio, Virgin Yield Stress and the Tangent Modulus. Since the molten

material resolidifies as a damage free material, any deformation occurring above the melting point will not affect the final residual stress. Based on this argument the temperature histories of the molten material are assumed to be limited by the melting temperature, thus only material properties up to the melting point are required for the stress analysis. The Virgin Yield Stress and the Tangent Modulus characterise the material behaviour in the Elastic-Plastic region. A zero Tangent Modulus must be specified at temperatures above melting so that the molten material can resolidify as a virgin material. The Virgin Yield Stress at melt should be very small but not zero as this will create division by zero problems in the computation. At the higher temperatures where the material is likely to behave in a highly plastic manner, the Elastic Modulus and the Poisson's Ratio have little influence on the calculated stresses, however the actual values adopted will significantly affect the solution convergence rate. The extent of thermal straining during welding is determined by the product of the Average Coefficient of Thermal Expansion and the maximum temperature change at a material point. Therefore it is important that accurate values of Coefficient of Thermal Expansion be used.

6.1.4 Restraint and Support Conditions

The analysis of welds in full three dimensions is possible with the finite element method however the cost would be excessive in a single pass weld and impractical in a multipass weld. Therefore to be economically feasible welds are analysed considering a two dimensional plane perpendicular to the welding direction. This necessitates assumptions to be made with respect to the displacements parallel to the weld direction or perpendicular to the plane of analysis. Chapter 3 has

indicated that various assumptions correspond to the prevention of certain modes of plate distortion. Chapter 3 also indicates that various assumptions (plane strain, generalized plane strain) perpendicular to the plane of analysis makes only a small change to the magnitude of residual stresses. Similarly only small changes occur in the distribution of residual stresses with the plane strain assumption leading to a slightly larger tensile zone. This indicates that with the exception of the start and end of the weld a two dimension analysis is sufficient for most purposes. A generalized plane strain analysis indicates rotations in the plane of analysis and can be used to give an estimate of plate bowing.

Chapter 3 also describes the results of various in-plane support or restraint conditions. In this case the degree of restraint substantially alters the transverse residual stress distribution and to a lesser extent the longitudinal residual stress distribution. Considerable restraint or support promotes the growth of tensile stresses in the transverse direction and by Poisson's effect in the longitudinal direction.

6.1.5 Results

The residual stress results described in Chapter 3 appear similar to those reported in a wide variety of studies: Tensile longitudinal stress in the weld area changing to compressive longitudinal stress away from the weld area.

The similarity of results for a variety of welding situations suggests one major mechanism for creating residual stress modified by the details of the specific welding processes. In general it appears that

residual stresses are created by,

- a) Compressive yielding due to temperature in the material surrounding the weld pool when the weld metal is introduced. These compressive plastic strains are trapped in this material when the weld metal solidifies giving an approximate stress distribution shown in figure 6.1.
- b) Once the weld metal has solidified, cooling of the weld metal causes tension to develop in the weld metal. Less tension is developed in the plastic zone since it has an initial compressive strain and is cooled from a lower temperature.
- c) The developing tensile zone causes metal away from the weld to develop compression to retain equilibrium of the cross-section. The final residual stress will have the approximate form shown in figure 6.2.
- d) Transverse stresses are also created but modified by distortion or by support of the plate in the plane of the paper.

Using this simple model the maximum longitudinal stress attained can be estimated by assuming that the contraction along the length of the weld is fully restrained by the adjacent material. For the 316L stainless steel used in this study the contraction strain ϵ_l^{th} can be calculated as,

$$\begin{aligned}
 \epsilon_l^{th} &= \Omega (T_{melt}) [T_{melt} - 20^\circ\text{C}] \\
 &= 0.22 \times 10^{-4} [1400^\circ - 20^\circ] \\
 &= 0.0304 \text{ m/m}
 \end{aligned}
 \tag{6.1}$$

where Ω is the coefficient of Expansion and 1400°C is the melting temperature

Using the room temperature Virgin Yield Stress (σ_y) and Tangent Modulus (E_t) the longitudinal stress can be estimated as,

$$\begin{aligned}\sigma_l &= \sigma_y + E_t [\epsilon_l^{th} - \epsilon^{el}] \\ &= 233 + 2800 [0.0304 - 0.0012] \\ &= 314 \text{ MPa}\end{aligned}\tag{6.2}$$

where ϵ^{el} is the elastic strain at yield (0.0012m/m).

Similarly the maximum transverse stress of a full penetration weld with the edges of the plate fixed can be estimated as follows. The transverse strain ϵ_t^{th} of the weld can be calculated as,

$$\begin{aligned}\epsilon_t^{th} &= \frac{W}{L} \alpha(T_{melt}) [T_{melt} - 20^\circ\text{C}] \\ &= \frac{1.2 \text{ cm}}{60 \text{ cm}} \times 0.22 \times 10^{-4} \times 1380^\circ\text{C} \\ &= 0.0006\end{aligned}\tag{6.3}$$

where W = width of the weld bead
 L = width of the plate section

Because ϵ_t^{th} is less than the elastic strain ($\epsilon^{el} = .0012$) at room temperature the transverse stress can be calculated as,

$$\begin{aligned}\sigma_t &= E \epsilon_t^{th} = 2 \times 10^5 \times 0.0006 \\ &= 120 \text{ MPa}\end{aligned}\tag{6.4}$$

This simple model gives values of stress very similar to those predicted by the finite element analyses supporting the idea that one mechanism for creating residual stress predominates. Variations in the welding process will alter the value and distribution of residual stress

somewhat but the general shape and trends will remain. Major modifications to transverse stresses can result from changes in restraint applied to the material either through support of surrounding material or through support of the weldment and distortion. As will be described, the concept of restraint of the surrounding material is a key element in the formation of transverse residual stresses in multipass welds.

6.2 MULTIPASS WELDS

As has been described, for weldments with more than two or three weld passes a method of simplification will be necessary to reduce the computing cost. The two and three pass welds described in Chapters 4 and 5 have been studied to determine the general formation of weld residual stress and to suggest the most likely method of simplifying welds with more passes.

In studying the results of the 2 and 3 pass welds described in Chapters 4 and 5 it appears that the distribution of longitudinal residual stress in multipass weldments is not strongly influenced by the sequence of welding or the restraint or support acting on the weldment. This does not appear to be the case for transverse residual stress and therefore it is convenient to discuss the formation of longitudinal and transverse residual stresses separately. In flat plates some coupling exists between the two stress directions because of Poisson's Ratio effects but this does not appear to be a major consideration in the results. This observation may not be true for girth welds of tubular products where there is stronger coupling between circumferential growth and longitudinal bending.

One notes that for the particular welding process studied the longitudinal residual stress is nearly four times larger than the transverse residual stress. Thus failure of a weld due to weld residual stress (e.g. cold cracking) is primarily one of the formation of cracks across the weld or perpendicular to the weld direction. Such cracks will generally not propagate since they are entering a region of compressive residual stress. However application of mechanical loading parallel to the weld direction would contribute to this type of failure and of particular concern must be bending in the plane of the weld.

The transverse residual stresses are much less likely to cause weld cracking due to their lower magnitude. However combined with mechanical loading or bending perpendicular to the weld and the stress concentration at the toe of the weld reinforcement, transverse residual stresses can raise some concern.

6.2.1 Multipass Weld Results - Longitudinal Residual Stress

The results of Chapters 2, 3 and 4 suggest that the longitudinal stress developed in a weld is primarily a function of the thermal contraction of the weld metal. Support of the plate in the longitudinal direction has only a small effect. Thus for longitudinal stress, strip models developed by various researchers (see Appendix A) are quite appropriate. In these models each strip and so each weld pass is considered to act independently. The results of the 2 and 3 pass welds described earlier appear to support this with each weld pass simply increasing the size of the tensile zone. The magnitude of the longitudinal stress increases only slightly with each weld pass and this appears to be due to a Poisson's effect from the transverse stresses.

It is believed that after a number of weld passes all the weld metal will retain a tensile longitudinal residual stress with a magnitude equal to that of a single pass weld. This is independent of the sequence and position of the various weld passes.

6.2.2 Multipass Weld Results - Transverse Residual Stress

The results of the transverse residual stress prediction of the 2 weld passes laid vertically and the 2 weld passes laid horizontally differ somewhat. When the passes were laid on top of each other each pass developed transverse residual stresses similar to that of a single pass weld. When the passes (including the three pass weld) were laid horizontally in a weld preparation the transverse residual stress was lower until the final weld pass filled in the cavity of the weld preparation. It would appear that transverse stresses will only develop when there is transverse restraint in the weld. For example consider figure 6.3 as representing a partially completed multipass weld. As each weld pass is applied little transverse stress will be created as there is little transverse restraint (assuming complete cooling between passes). However when the final pass is made to completely fill the cavity it will be restrained by the other passes and parent metal. This will induce transverse residual stress in the final pass and in adjacent material. This in turn may cause weldment distortion and some relief of these stresses.

In the above arrangement the weld metal of the last pass will develop a tensile transverse residual stress. If the plate is not restrained in the transverse direction compression will be generated in the parent metal. The trend will be tensile transverse residual

stresses on the top of the plate and compression on the bottom leading to a bending type of distortion and some relief of the residual stresses. If distortion is prevented through clamping or simply the size of the weldment the maximum restraint and transverse residual stress will be produced.

6.3 EXTENSION OF THE RESULTS

The results of this study appear to be directly applicable to multi-pass welds where weld metal is placed into a cavity. It is suggested that the longitudinal stress will reach a maximum value in each weld pass independent of the number of passes, their location and the sequence of application. Thus each pass simply expands the zone of longitudinal tensile stress but not the magnitude. The size and magnitude of the compressive stress zone will change accordingly.

As each layer in the cavity is completed a transverse residual stress will be created. Thus there is a good argument for grouping weld passes into layers and considering the application of each layer. However the method of considering all passes in a layer being applied simultaneously used by other researchers (Appendix A) is not correct. A two step procedure for each layer is required in which each pass up to the last pass creates little transverse residual stress and then the last passes (closing pass) creates a local transverse residual stress. The exact method by which this could be accomplished has not been determined. By considering each layer separately one could simulate the gradual distortion of the plate and the gradual release of transverse stresses due to plate distortion. This could be quite significant for deep penetration welds.

Of particular interest would be the last layer or capping passes which produce the weld reinforcement. Many of the passes would not be restrained and therefore the overall transverse stresses in the reinforcement would be small. This would suggest that transverse residual stresses created at the weld toe are primarily due to the weld passes below the capping passes. This requires further investigation.

Extending the results to multipass fillet welds leads to a similar consideration. For long welds the size of the longitudinal residual stress zone but not the magnitude should increase with each weld pass. The final distribution of longitudinal residual stress may vary somewhat from that of a flat plate as the zone of compressive stress will be different due to the fillet geometry. The transverse residual stress will at worst be equal to the stress created by welding into a cavity. More likely it will be modified by the flexibility or reduced restraint of the weldment geometry. The weld pass sequence and the number of passes will also influence the transverse residual stresses. However it appears feasible that further study considering fillet weld could lead to simplifying procedures.

Chapter VII

CLOSURE

A study of 2 and 3 pass welds placed into a weld preparation in austenitic stainless steel plates have indicated that residual stress predictions can be made on similar weldments containing many more passes.

The limited study suggests that longitudinal residual stresses in multipass welds are similar to those in single pass welds. With respect to longitudinal residual stresses the effect of each weld pass can be superimposed to increase the size of the residual stress zone but not the magnitude. Thus the longitudinal residual stress distribution appears to be only slightly affected by the welding pass sequence and location.

Unfortunately it appears that the distribution of transverse residual stress does depend on the sequence and position of the weld passes. To accurately calculate transverse residual stresses the transverse restraint must be modelled which will depend on the support of the weldment and restraint provided by other weld passes and the parent metal of the weld preparation. Further study is required to consider the effect of partially restrained layers such as will occur in weld reinforcing passes and fillet welds.

In considering multipass welds other researchers have recommended that weld passes be grouped into layers to reduce the number of passes to be considered. This appears wise however the grouping of all passes in a layer will not approximate the restraint properly as it appears that the last or closing pass creates the majority of the transverse

residual stress. Further study is required to develop a more accurate layer grouping technique. It would not appear to be wise to group weld passes in a vertical or through thickness sense.

TABLE 2.1
DOUBLE ELLIPSOIDAL GEOMETRY PARAMETERS

Quadrant	a	b	c
Front	0.6	0.6	0.3
Rear	0.6	0.6	3.7

(all dimensions in mm)

TABLE 2.2
LATENT HEAT OF FUSION FOR 316L STAINLESS STEEL

ELEMENT	H (cal/g)	316L	SANDVIK (weight %)	EMR
Fe	65.000	65.395	63.118	62.905
Cr	79.000	17.000	19.300	19.500
Ni	71.000	12.000	13.110	13.100
Mo	69.000	2.500	2.180	2.180
Si	430.000	1.000	.320	.300
Mn	64.000	2.000	1.750	1.730
P	4.800	.045	.007	.007
S	9.200	.030	.009	.008
C	.000	.030	.016	.017
Ti	100.000	—	.010	—
Co	66.000	—	.050	.049
V	98.000	—	.022	.030
Cu	49.000	—	.050	.034
N	6.200	—	.038	—
Nb	68.000	—	.010	.140
Ta	41.000	—	.010	—
H* 316L (cal/g)		71.767	69.685	69.670
(J/g)		300.272	291.564	291.501

H = Latent heat of fusion of individual elements from [2.25]

H* = Latent heat of fusion for 316L stainless steel
= sum of (H x Weight%) of all elements divided by 100

TABLE 2.3
TYPICAL COMPOSITION OF 316L STAINLESS STEEL PLATE AND ER316L
STAINLESS STEEL WELDING WIRE

ELEMENT	316L	ER316L	
		NOMINAL SANDVIK ANALYSIS	ACTUAL EMR ANALYSIS
C	0.030	0.016	0.017
Si	1.000	0.320	0.300
Mn	2.000	1.750	1.730
P	0.045	0.007	0.007
S	0.030	0.009	0.008
Cr	16.0-18.0	19.300	19.500
Ni	10.0-14.0	13.110	13.100
Mo	2.0-3.0	2.180	2.180
Ti	—	0.010	—
Co	—	0.050	0.049
V	—	0.022	0.030
Cu	—	0.050	0.034
N	—	0.038	—
Nb	—	0.010	0.140
Ta	—	0.010	—
Fe	balance	balance	balance

TABLE 2.4
WELDING PARAMETERS

Voltage	34 Volts
Current	440 - 450 Amps
Travel Speed	88.9 cm/min
Stick Out	2.54 cm
Wire Feed	3.175 cm/sec

TABLE 3.1

SUMMARY OF DEFORMATIONS OF A SIMPLY SUPPORTED PLATE
UNDER DIFFERENT OUT-OF-PLANE - RESTRAINT ASSUMPTION

OUT-OF-PLANE Restraint assumption		OUT-OF-PLANE		IN-PLANE
		Δu_z (cm)	ϕ_x (rad)	ϕ_y (rad)
generalized plain strain	case 1	-5.8055E-04	-2.2513E-05	-2.9069E-05
	case 2	-1.2868E-04	-2.3031E-05	0.0
	case 3	-1.4387E-04	0.0	0.0
zero plane strain	case 4	0.0	0.0	0.0
				Δu_y bottom centre
				-0.1415
				-0.1409
				-0.1410
				-0.1412

$P(x,y,z)$ = FIXED FRAME CARTESIAN
COORDINATES

$P(x,y,\xi)$ = MOVING FRAME CARTESIAN
COORDINATES

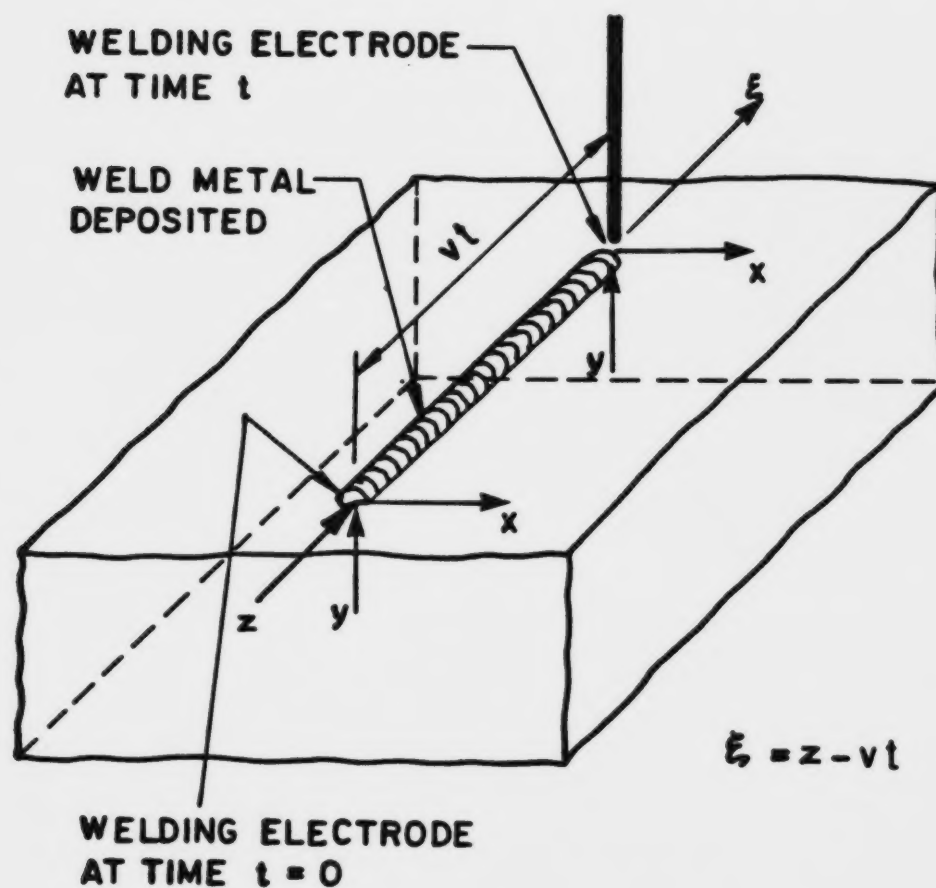


FIGURE 2.1 WELDMENT CONFIGURATION

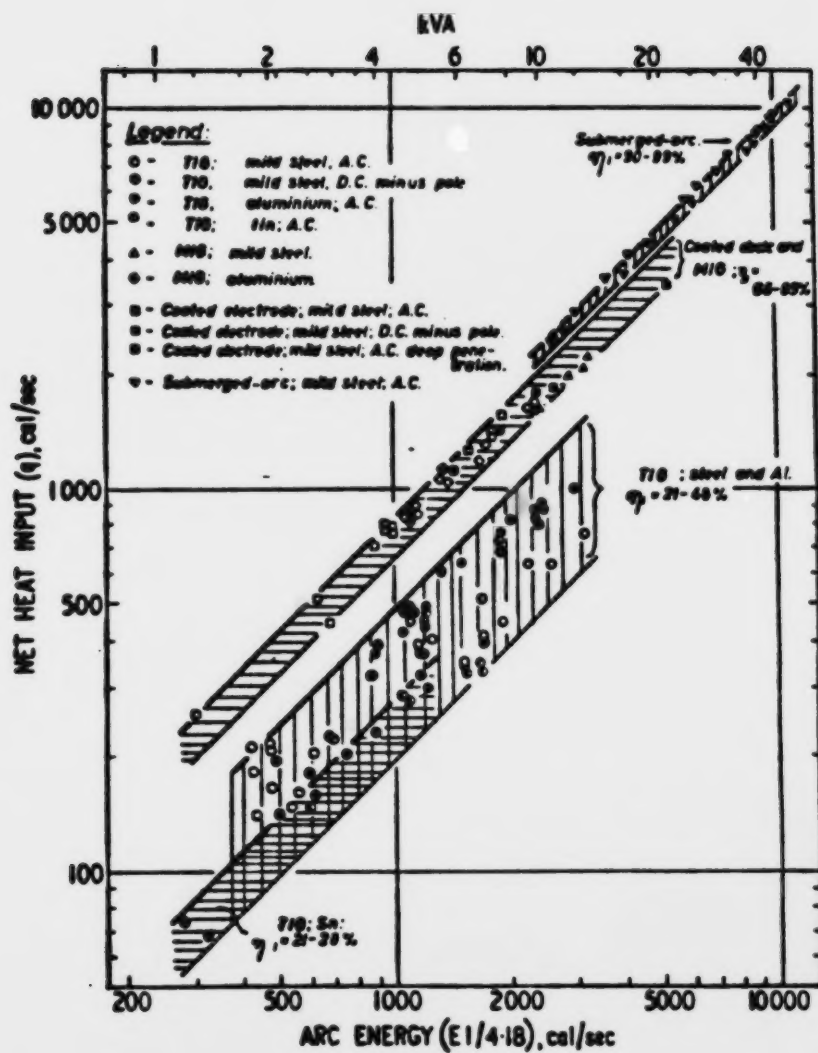


FIGURE 2.2 ARC EFFICIENCIES OF DIFFERENT WELDING PROCESSES FROM [2.9]

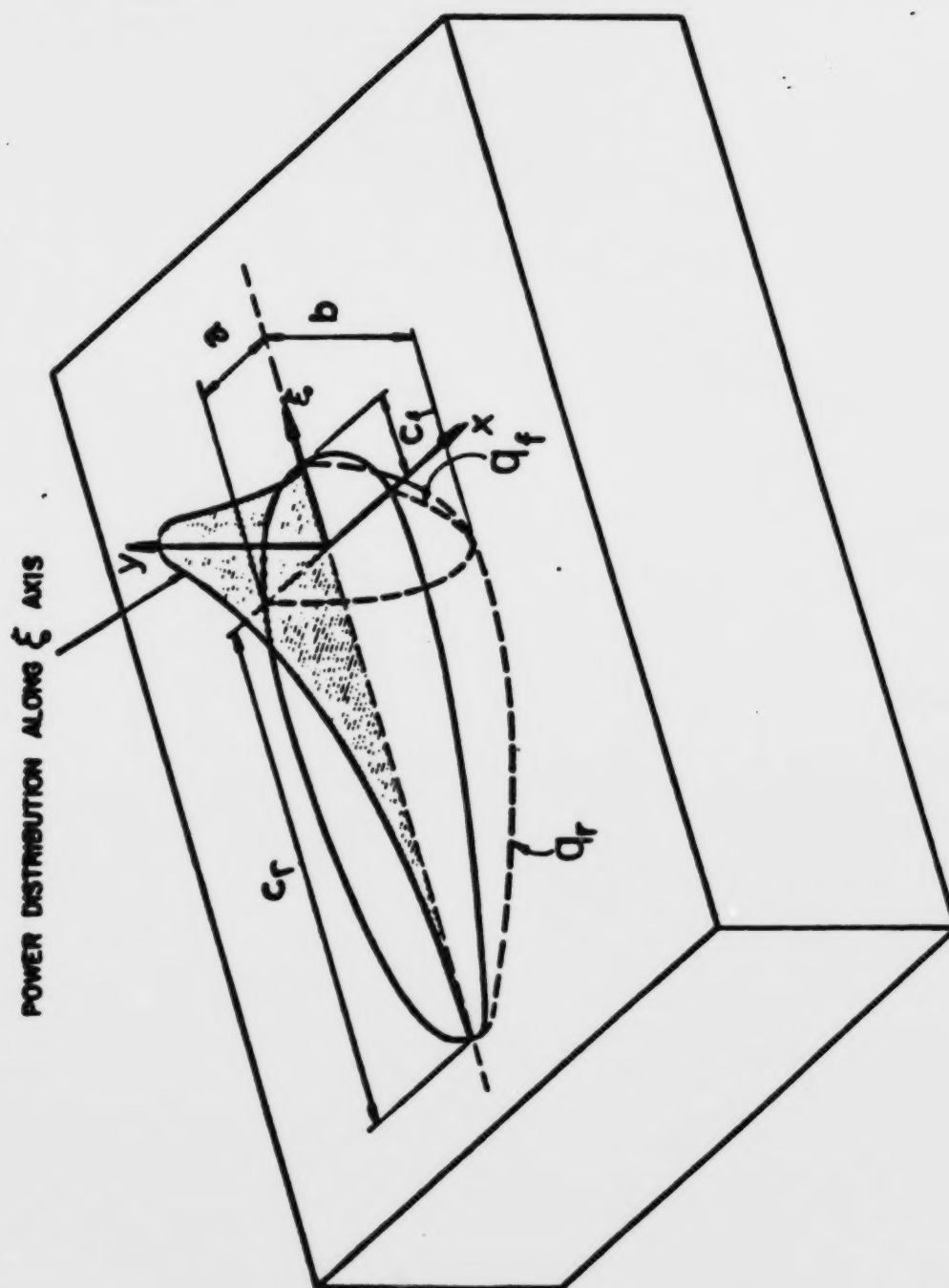


FIGURE 2.3. DOUBLE ELLIPSOIDAL HEAT SOURCE MODEL FROM [2.13]

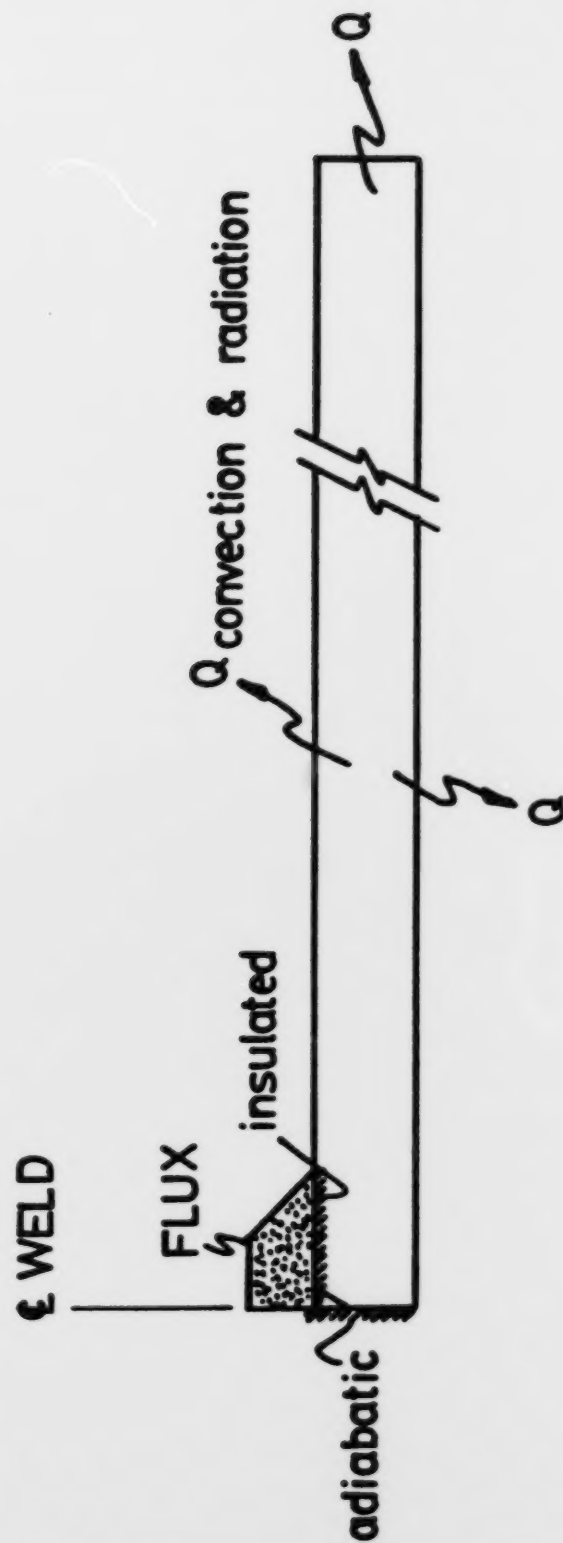


FIGURE 2.4. THERMAL BOUNDARY CONDITIONS OF A SAW WELDED PLATE

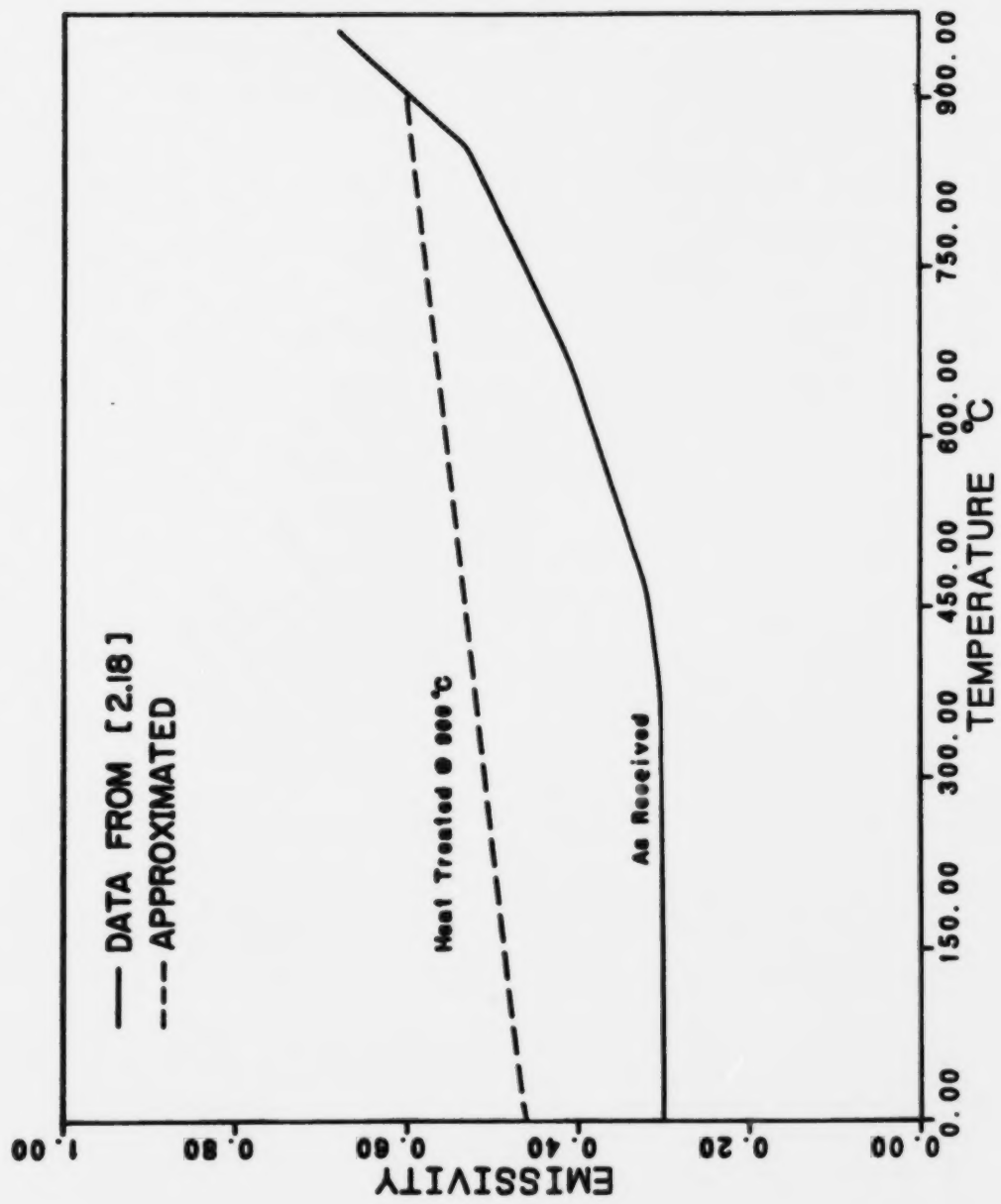


FIGURE 2.5. EMISSIVITY OF 316L STAINLESS STEEL

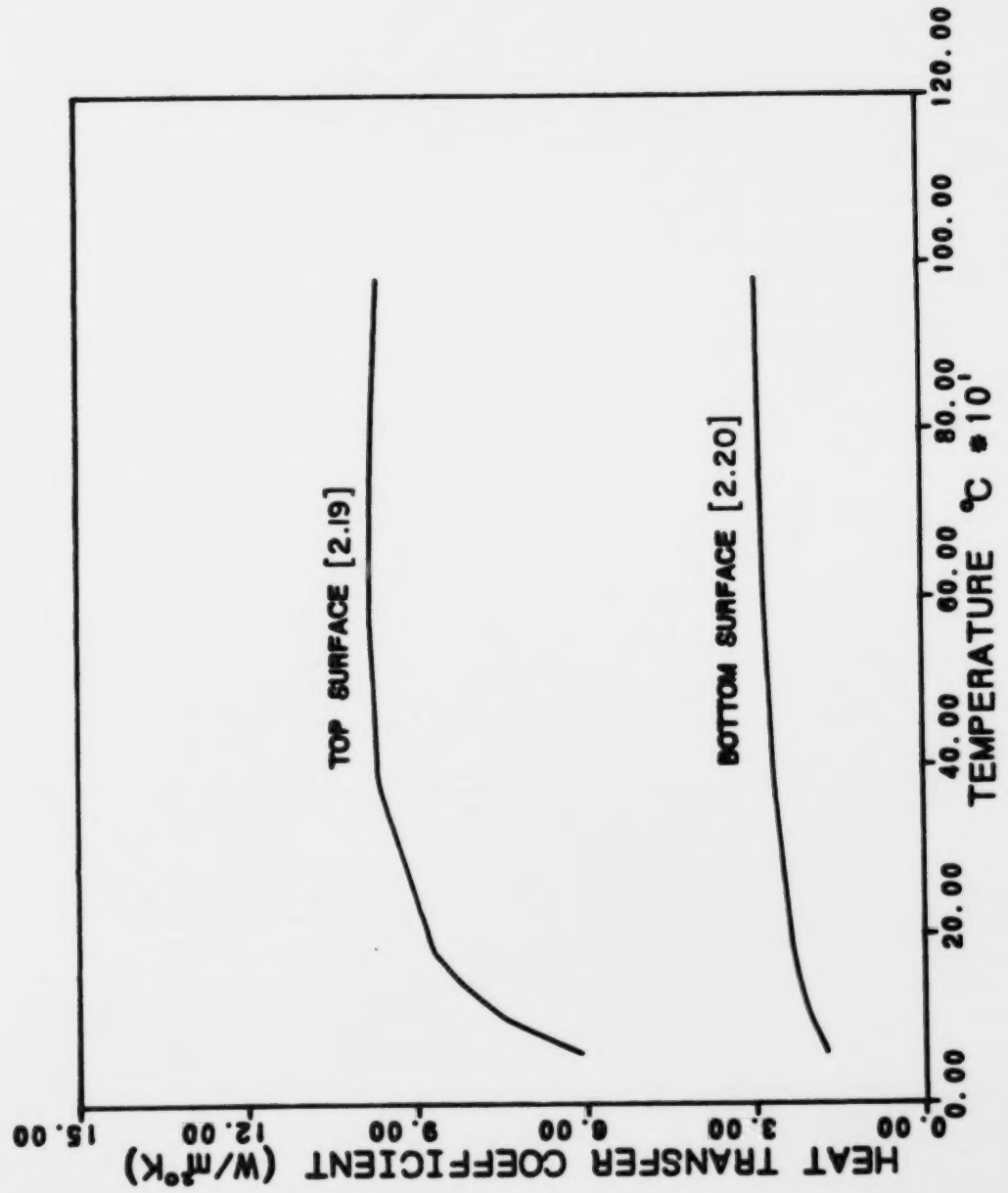


FIGURE 2.6. CONVECTIVE HEAT TRANSFER COEFFICIENTS FOR ISOTHERMAL SURFACES - ANALYTICAL

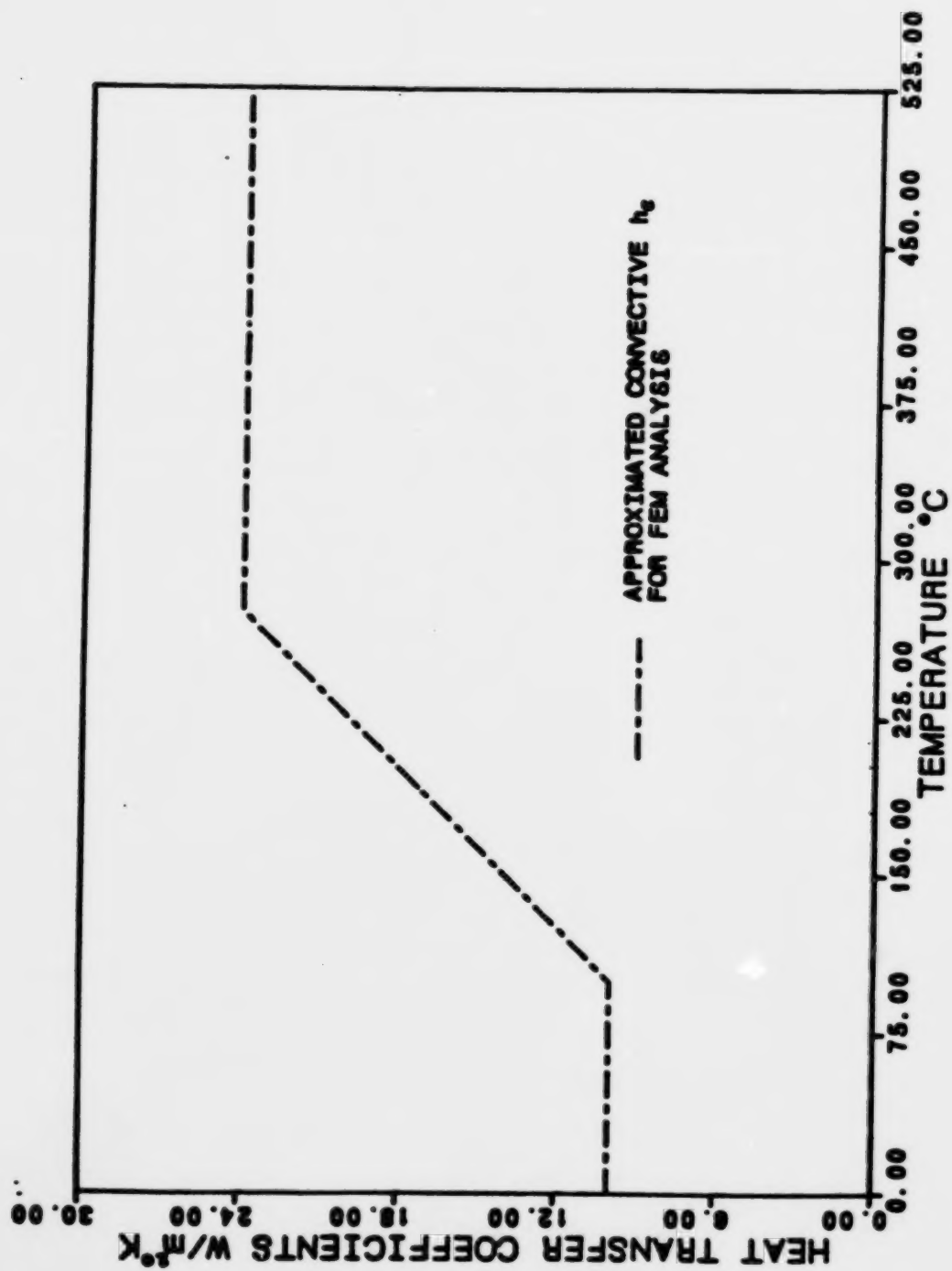


FIGURE 2.7. CONVECTIVE HEAT TRANSFER COEFFICIENTS ON A WELDMENT SURFACE
(FROM APPENDIX D)

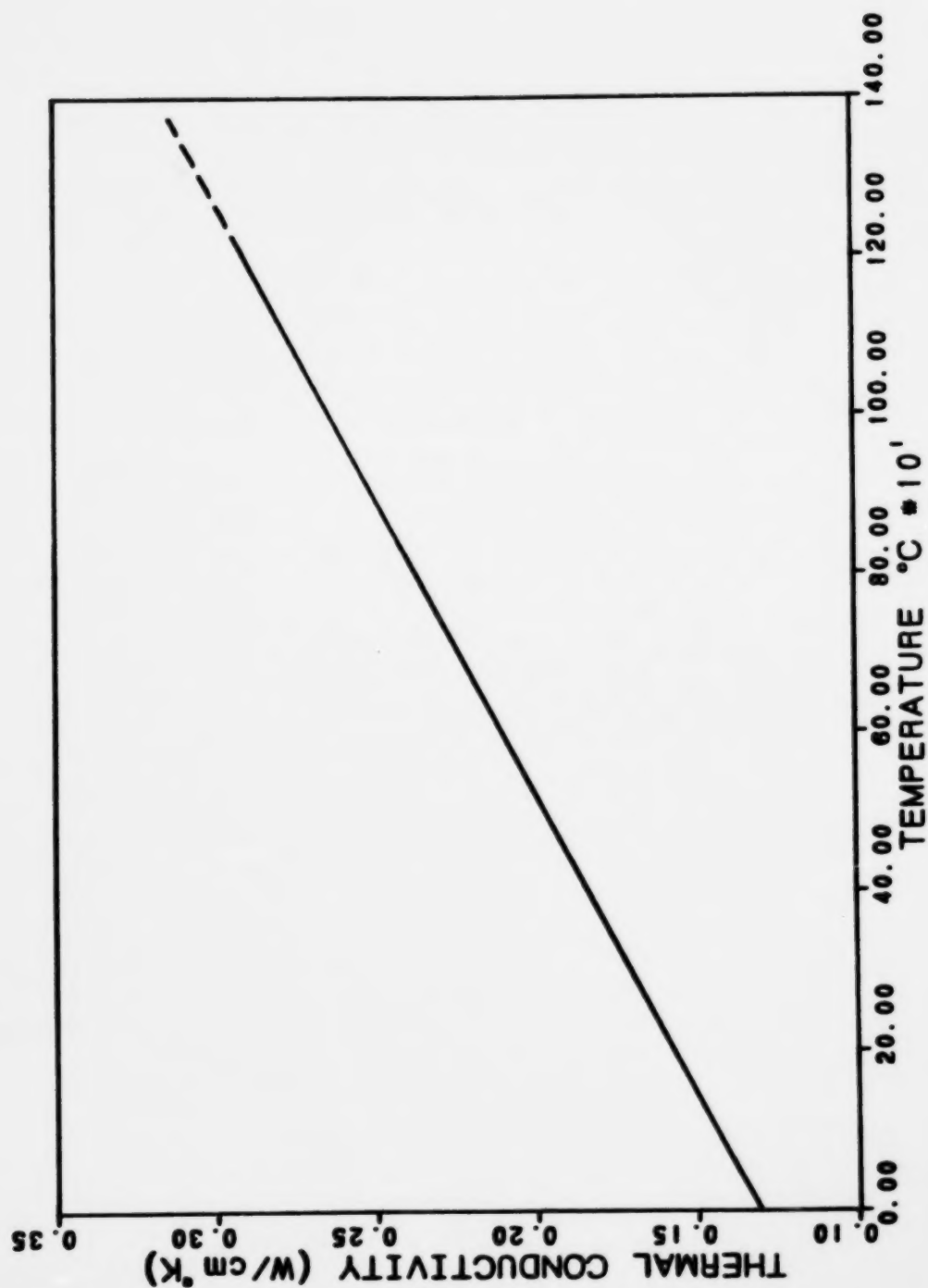


FIGURE 2.8. THERMAL CONDUCTIVITY OF 316L STAINLESS STEEL

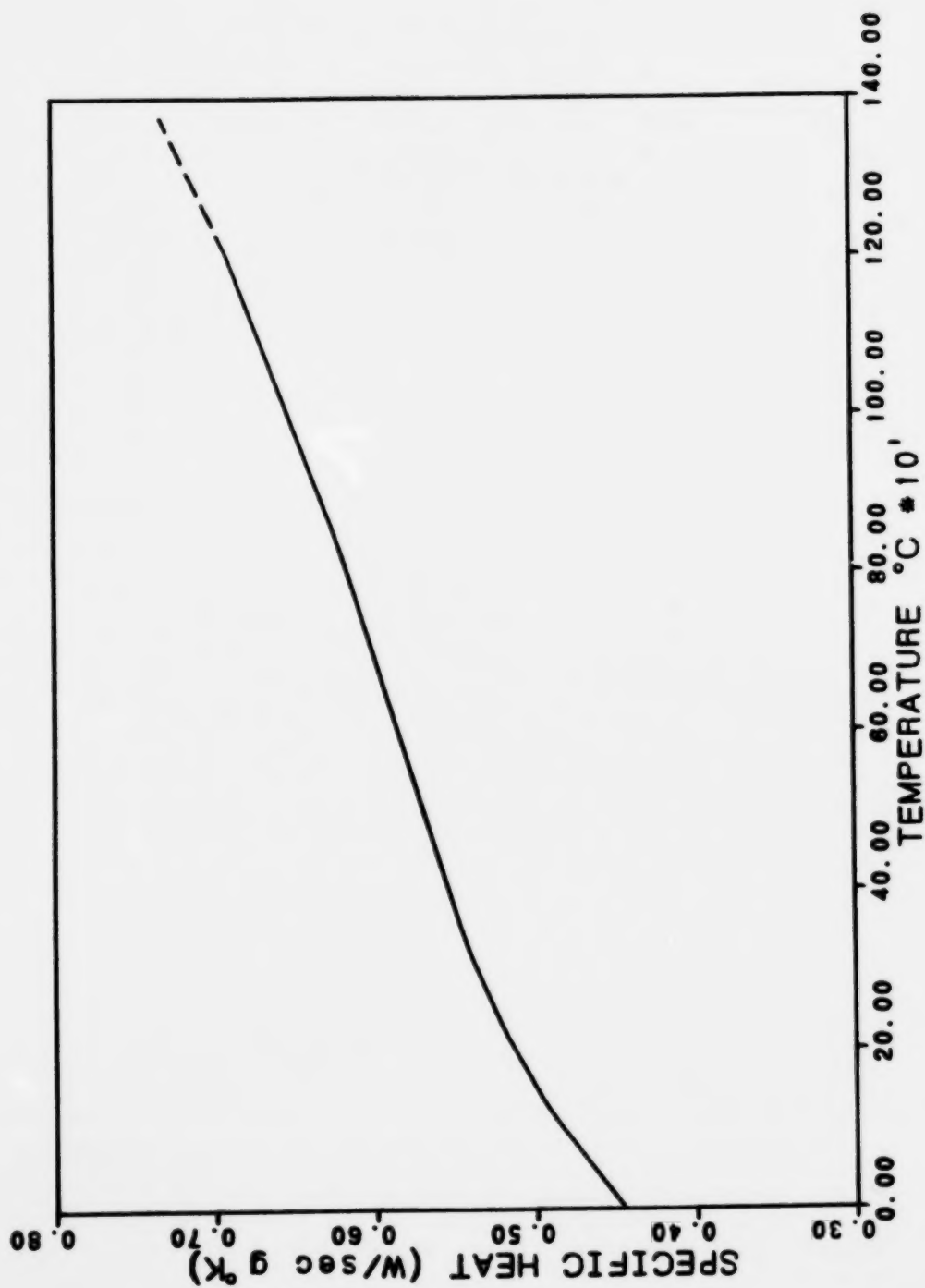


FIGURE 2.9. SPECIFIC HEAT OF 316L STAINLESS STEEL

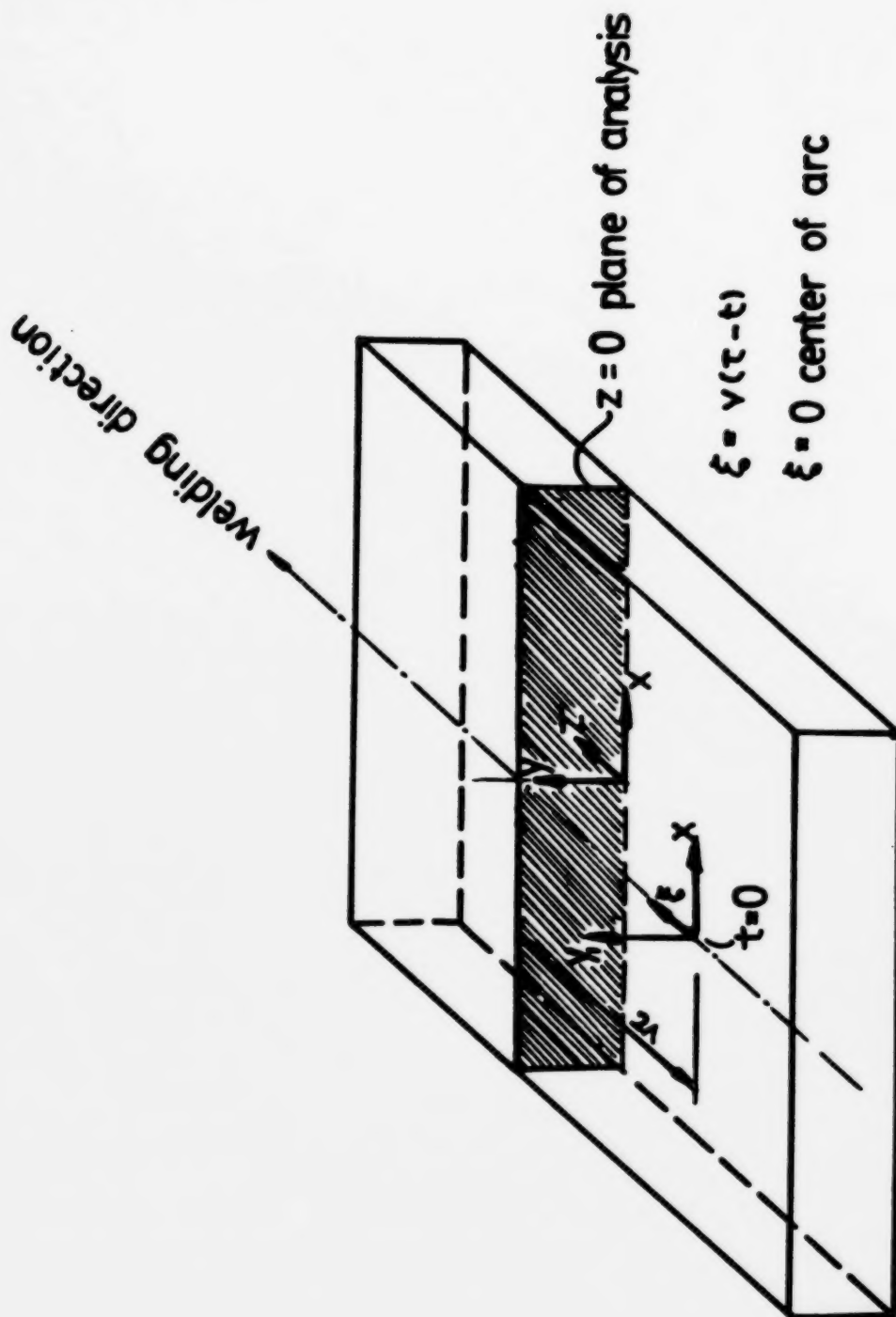


FIGURE 2.10. A PLANE PERPENDICULAR TO THE WELD FOR 2-D HEAT FLOW ANALYSIS

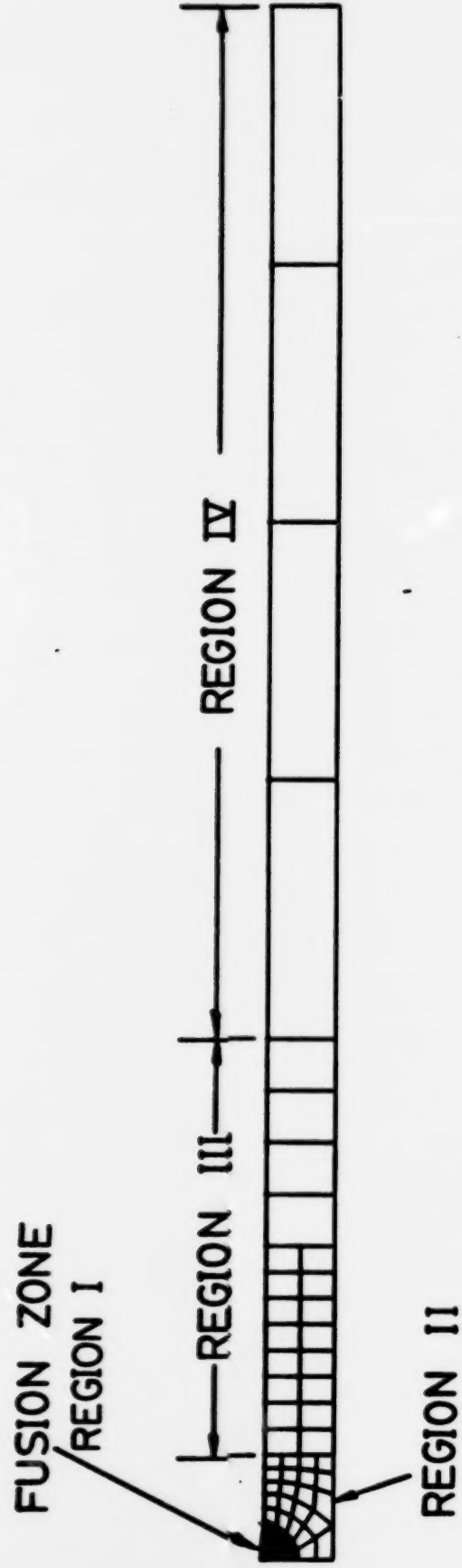


FIGURE 2.11. COMPLETE FINITE ELEMENT MESH FOR HEAT FLOW ANALYSIS

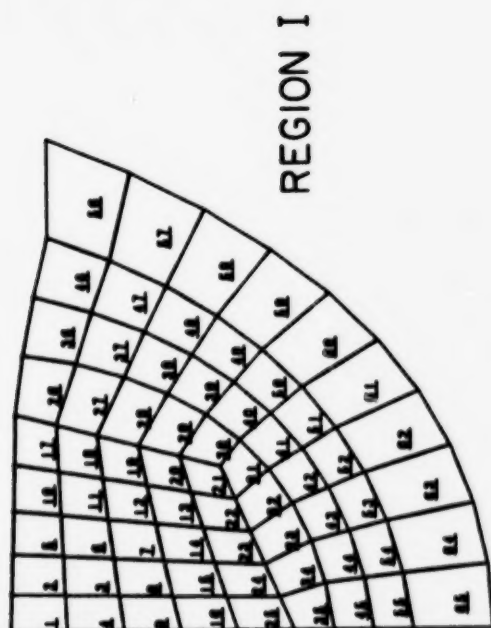


FIGURE 2.12. FINITE ELEMENT MESH IN REGION I - 4 NODDED ELEMENTS

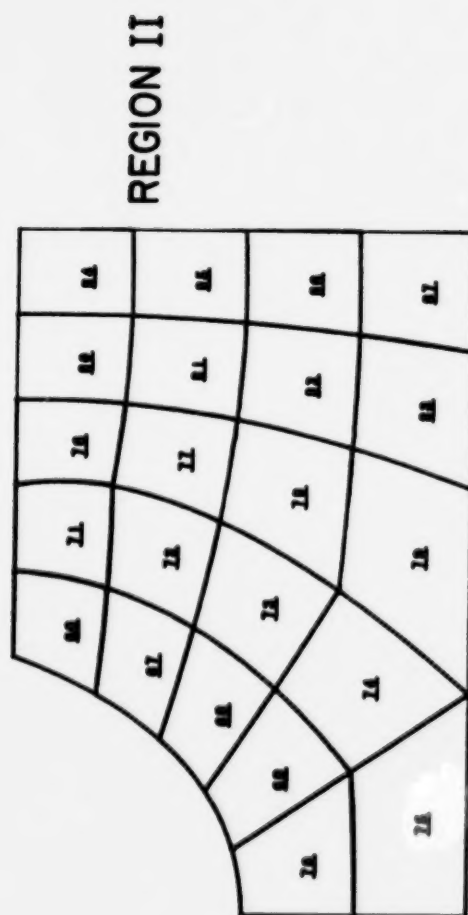


FIGURE 2.13. FINITE ELEMENT MESH IN REGION II - 8 NODED ELEMENTS

REGION III

101	102	103	104	105	106	107
108	109	110	111	112	113	114

FIGURE 2.14. FINITE ELEMENT MESH IN REGION III - 8 NODDED ELEMENTS

REGION IV

108	109	110	111
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FIGURE 2.15. FINITE ELEMENT MESH IN REGION IV - 8 NODDED ELEMENTS

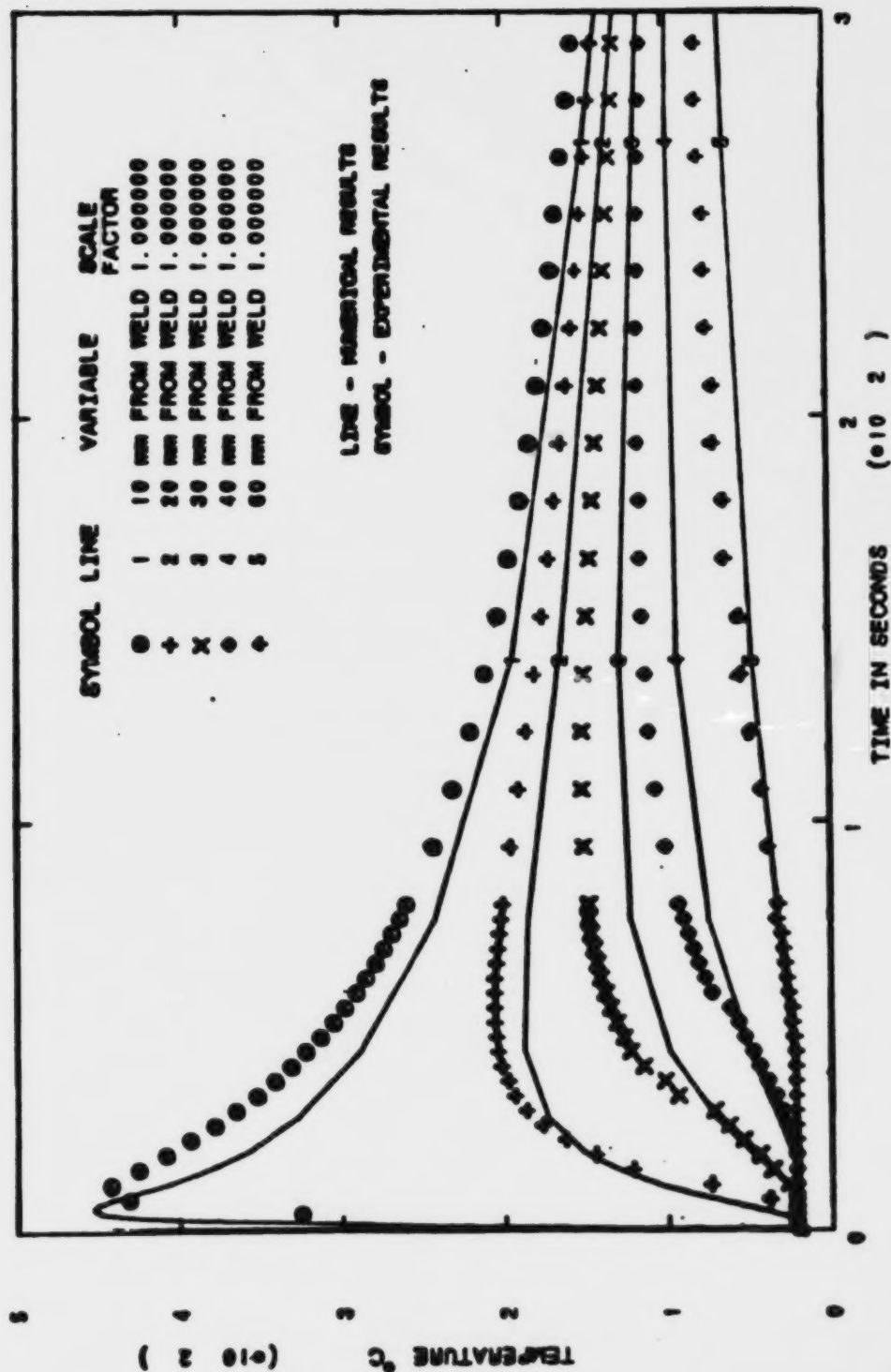


FIGURE 2.16. TEMPERATURE HISTORY ON THE TOP SURFACE OF PLATE DURING SAW WELDING
NUMERICAL ANALYSIS RESULTS

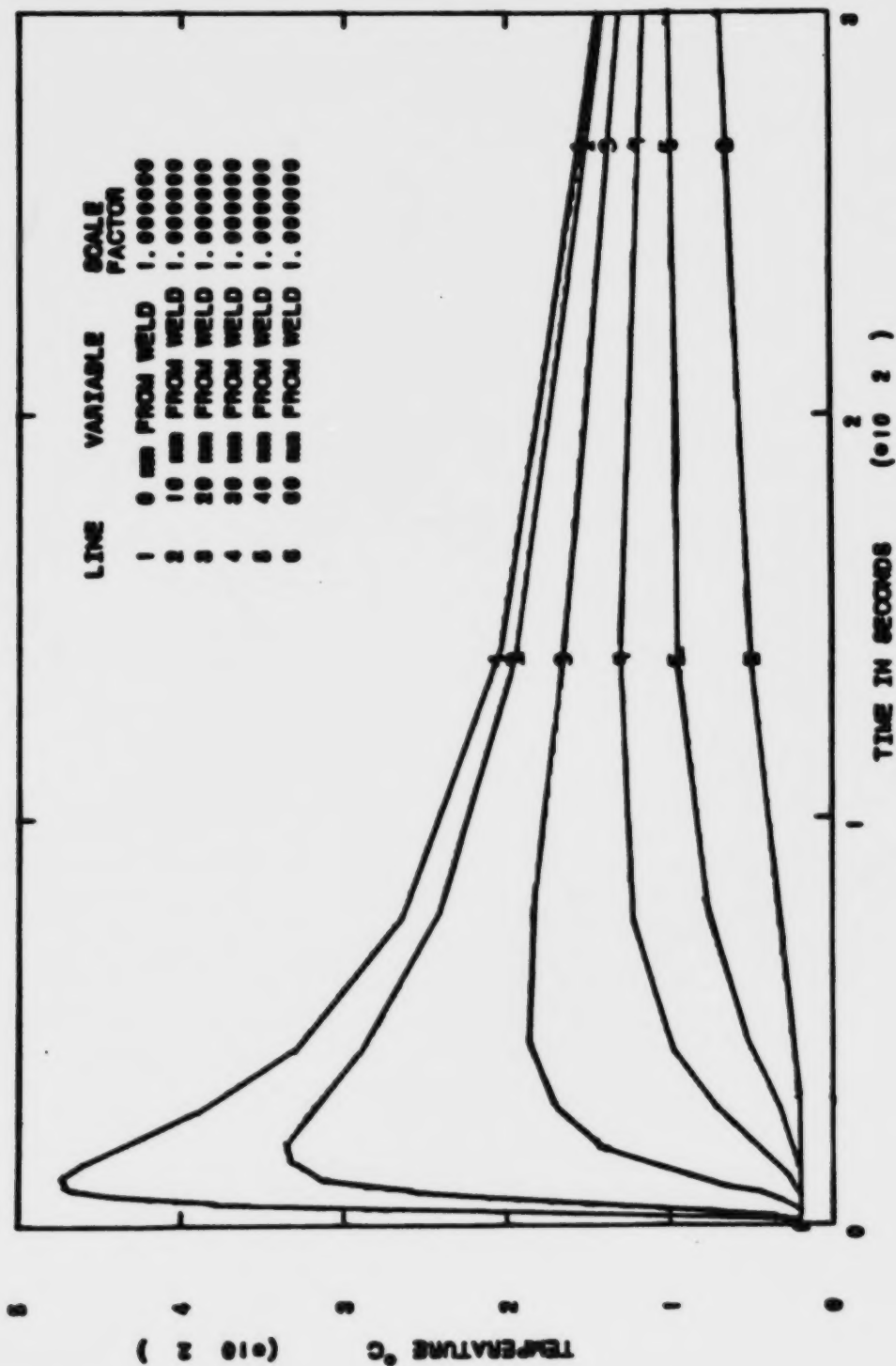


FIGURE 2.17. TEMPERATURE HISTORY ON THE BOTTOM SURFACE OF PLATE DURING SAW WELDING
NUMERICAL ANALYSIS RESULTS

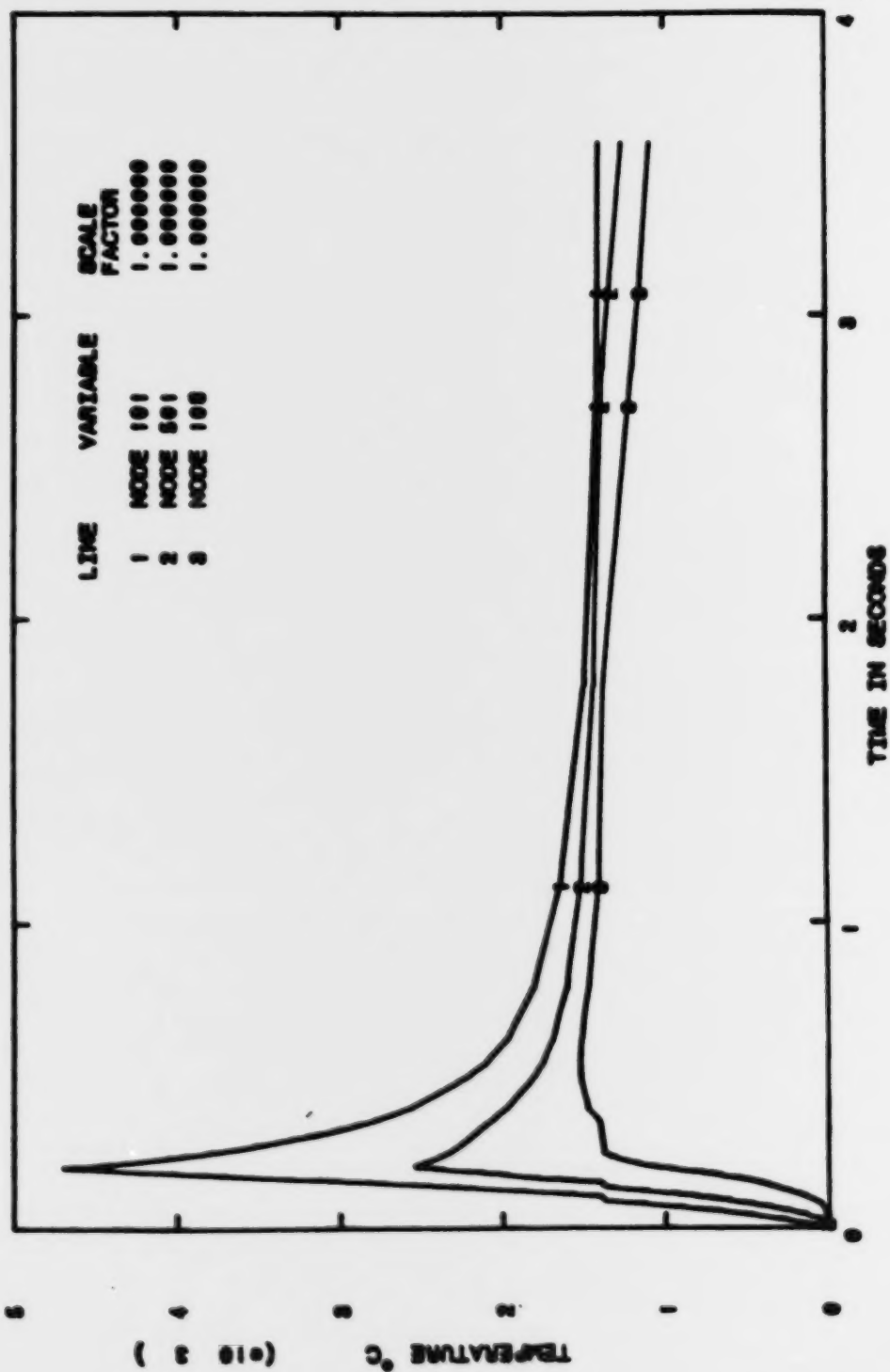


FIGURE 2.18. TEMPERATURE HISTORY INSIDE THE WELD POOL DURING SAW WELDING
NUMERICAL ANALYSIS RESULTS

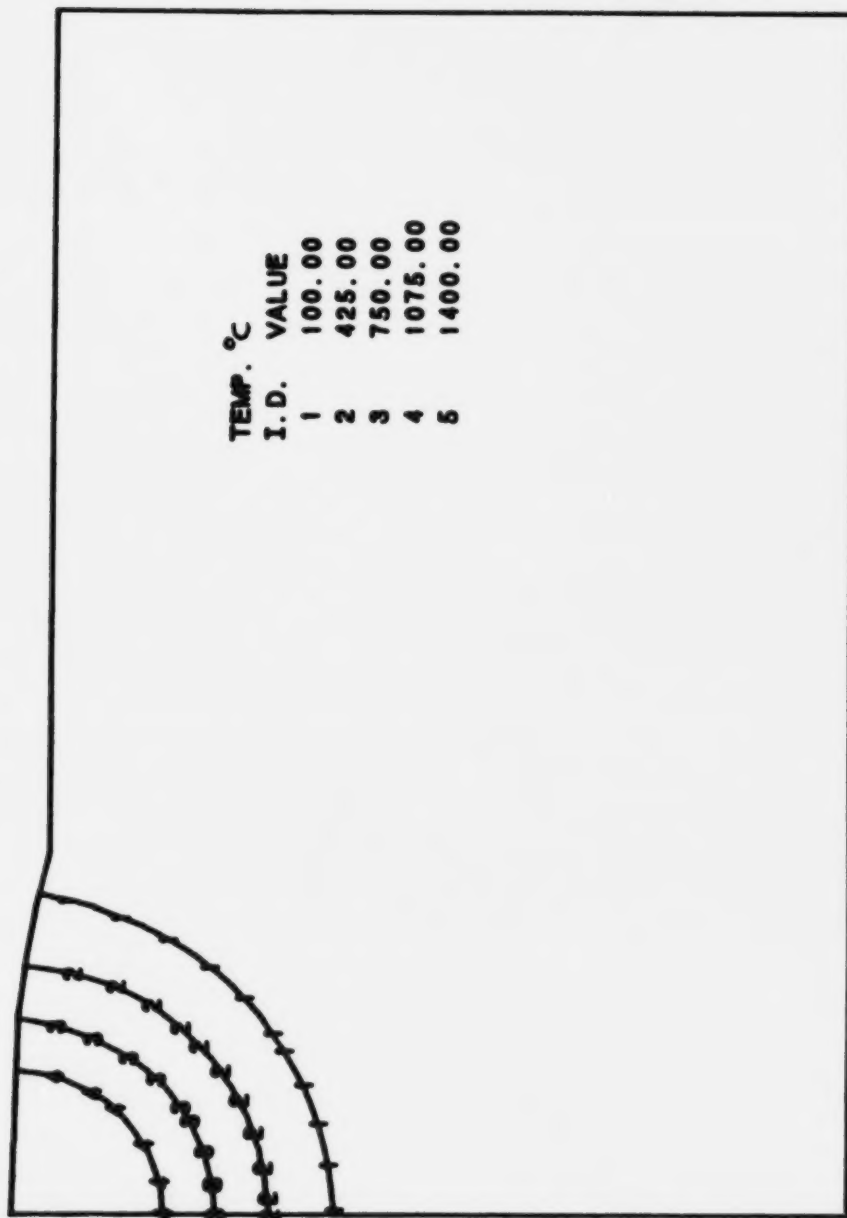


FIGURE 2.19. TEMPERATURE CONTOURS AROUND THE WELD POOL (TIME=0.099secs)

STEP 1 INCREMENT 10

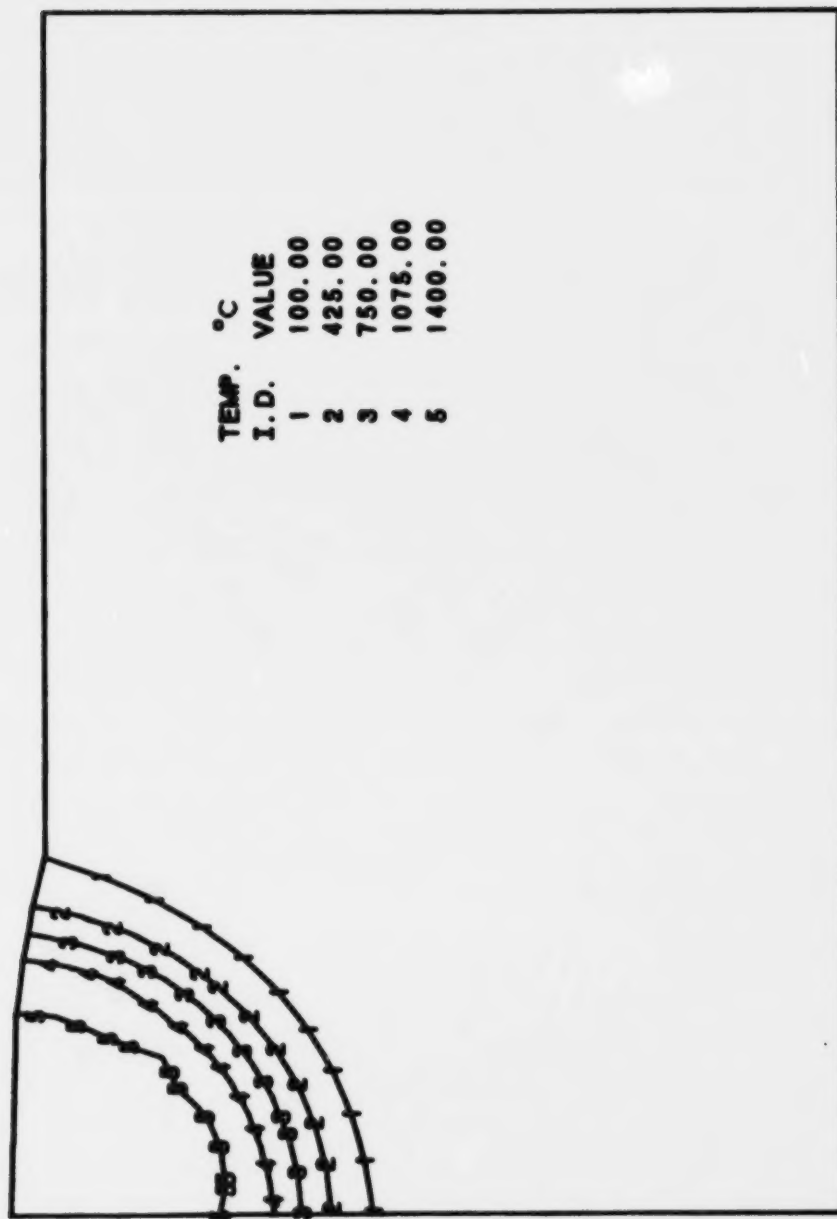


FIGURE 2.20. TEMPERATURE CONTOURS AROUND THE WELD POOL (TIME=0.150secs)

STEP 1 INCREMENT 20

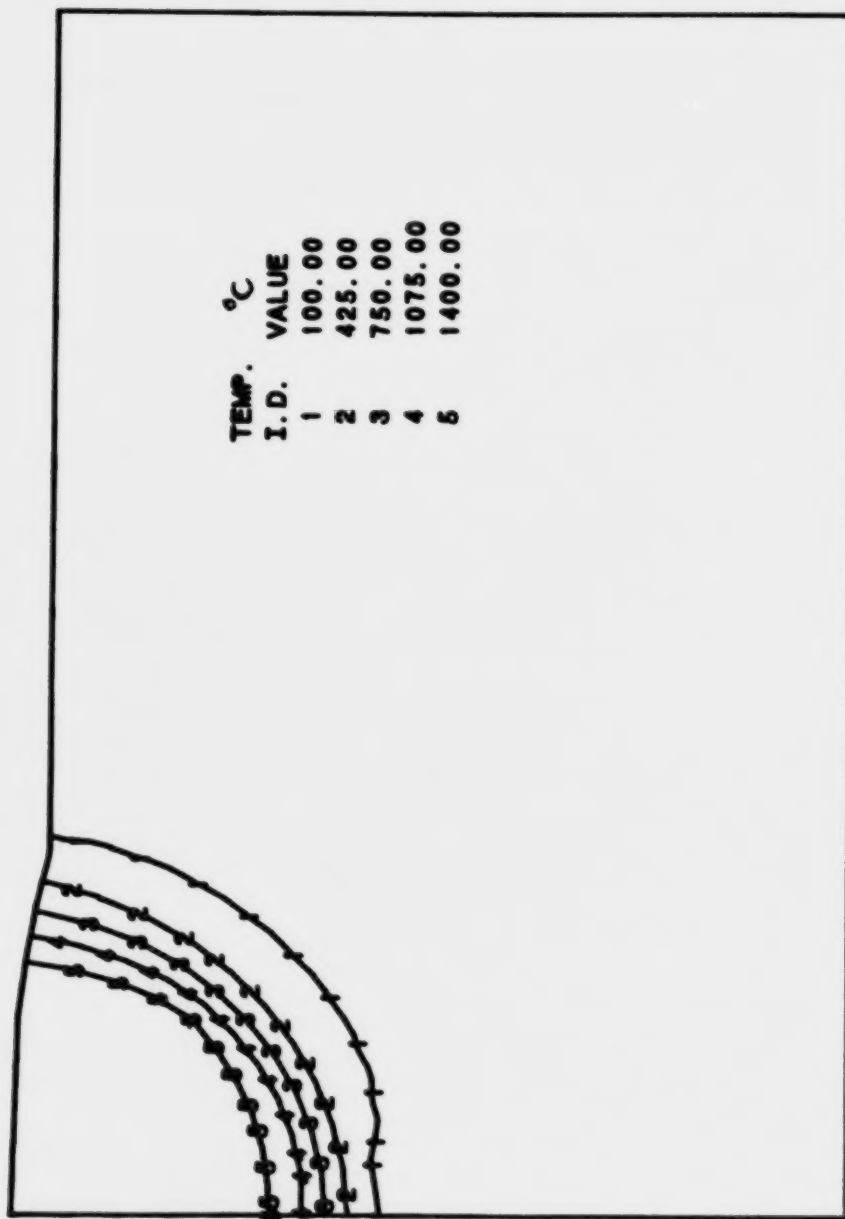


FIGURE 2.21. TEMPERATURE CONTOURS AROUND THE WELD POOL (TIME=0.176secs)

STEP 1 INCREMENT 25

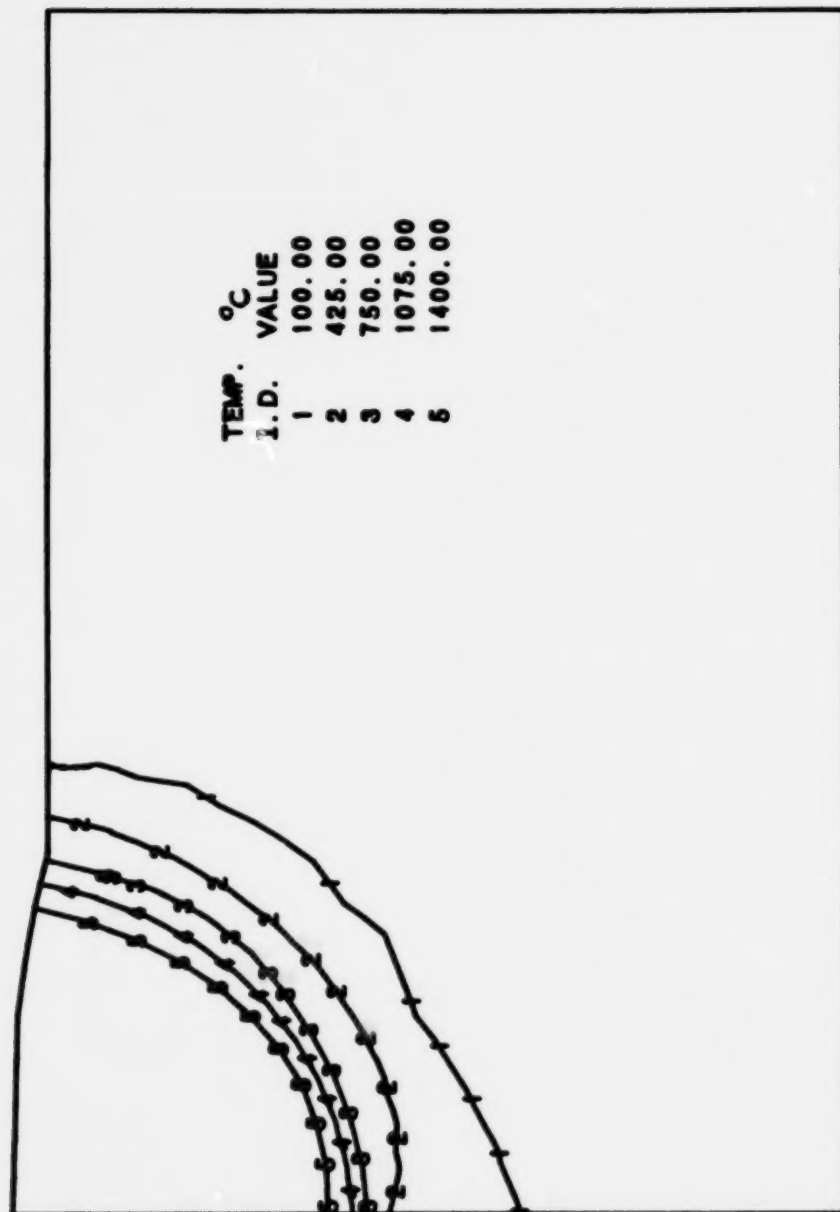


FIGURE 2.22. TEMPERATURE CONTOURS AROUND THE WELD POOL (TIME=0.288secs)

STEP 1 INCREMENT 35

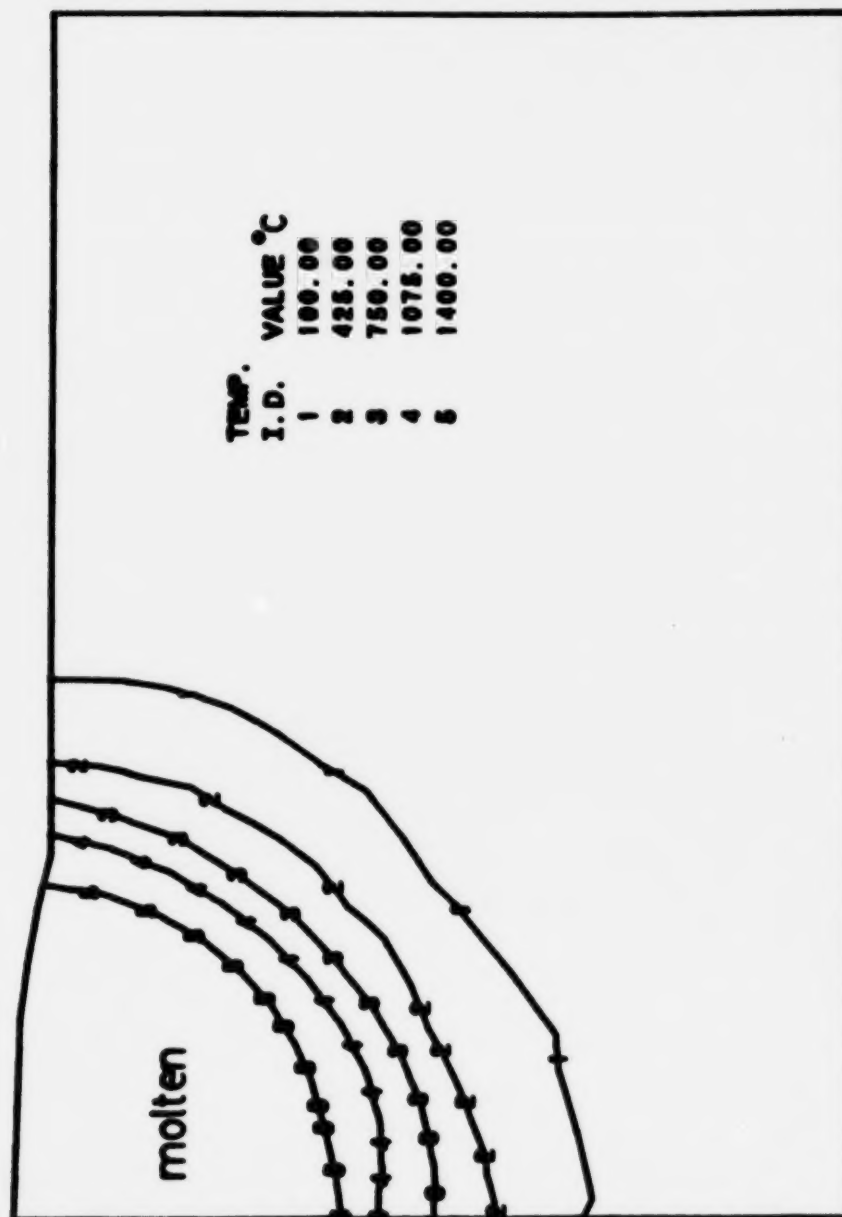


FIGURE 2.23. TEMPERATURE CONTOURS AROUND THE WELD POOL (TIME=0.536secs)

STEP 1 INCREMENT 48

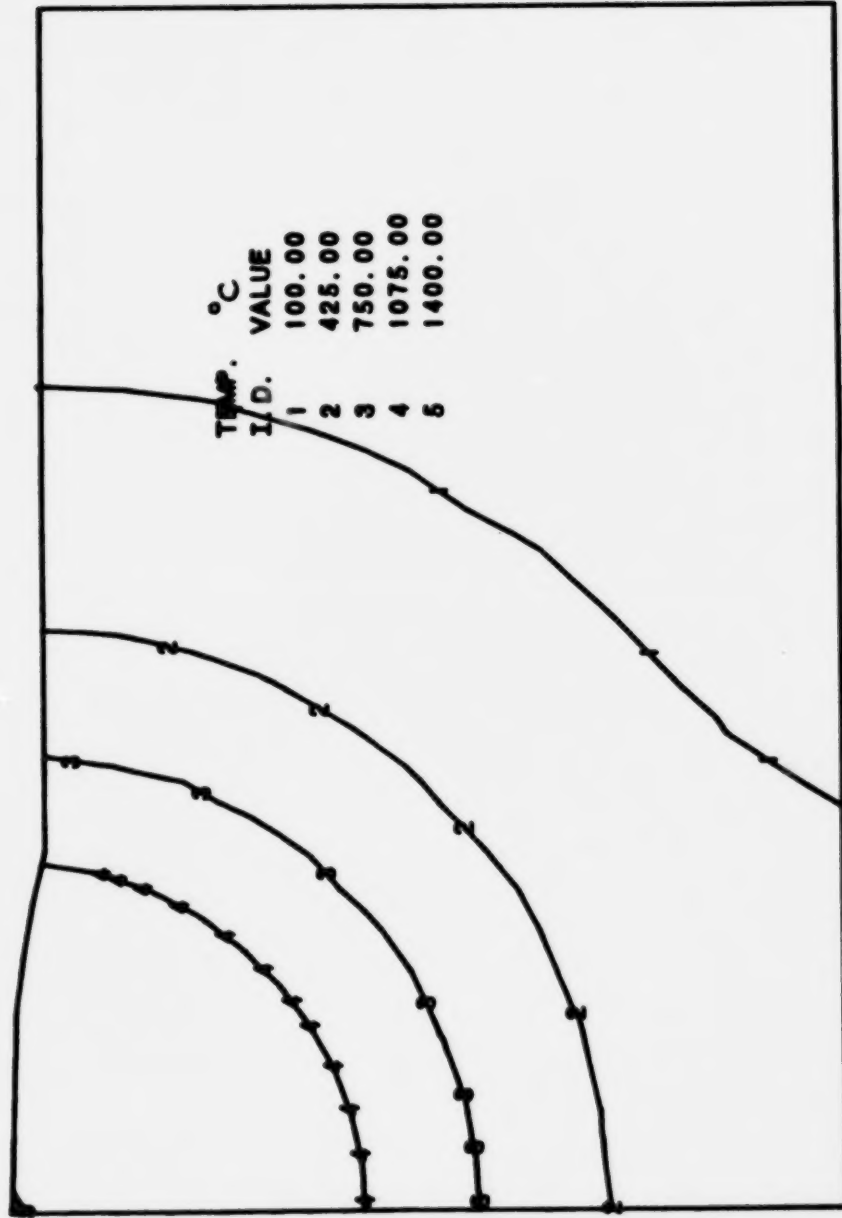


FIGURE 2.24. TEMPERATURE CONTOURS AROUND THE WELD POOL (TIME=2.700secs)

STEP 1 INCREMENT 50

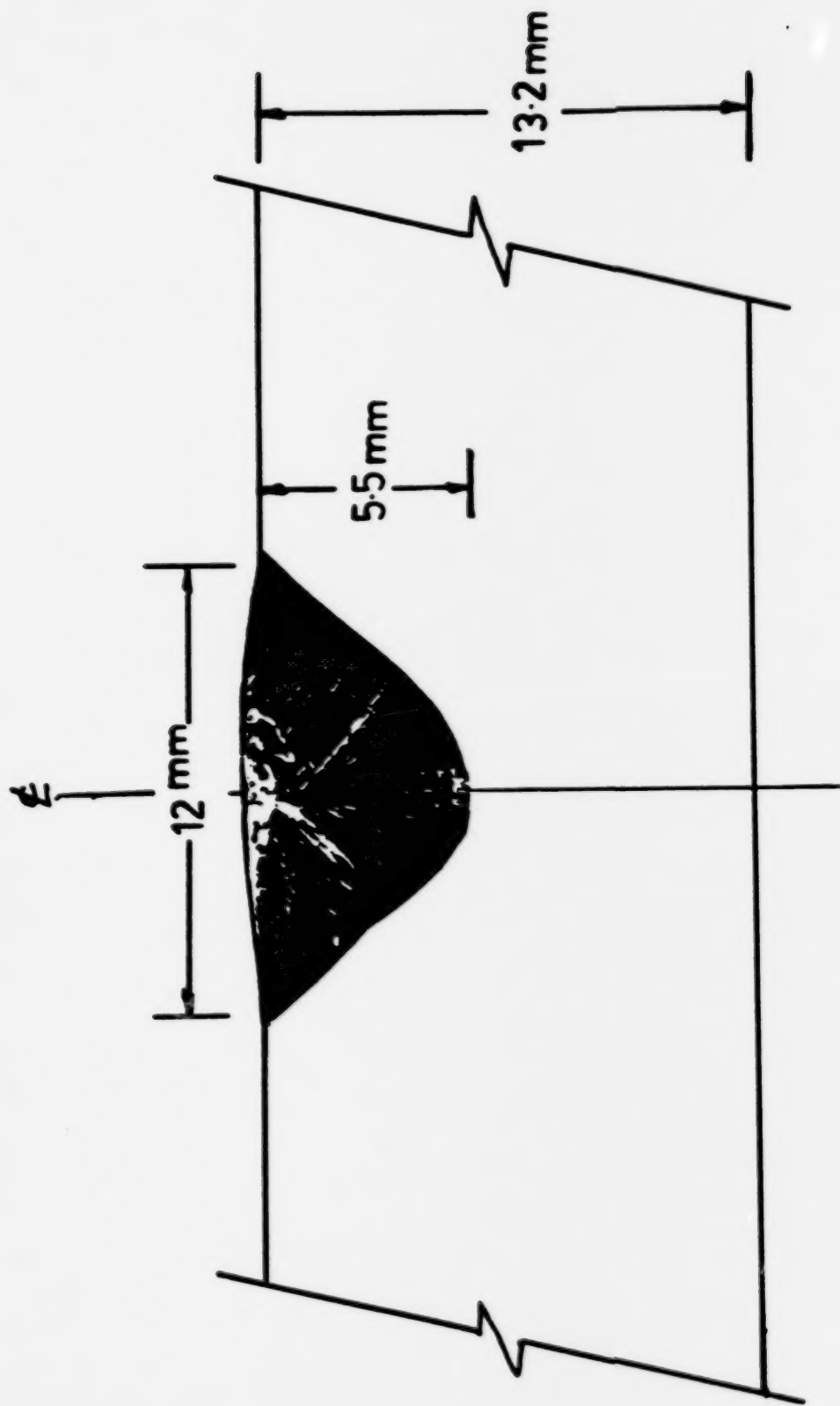


FIGURE 2.25. A TYPICAL PHOTOMACROGRAPH OF AN ETCHED CROSS SECTION OF A SAW WELD

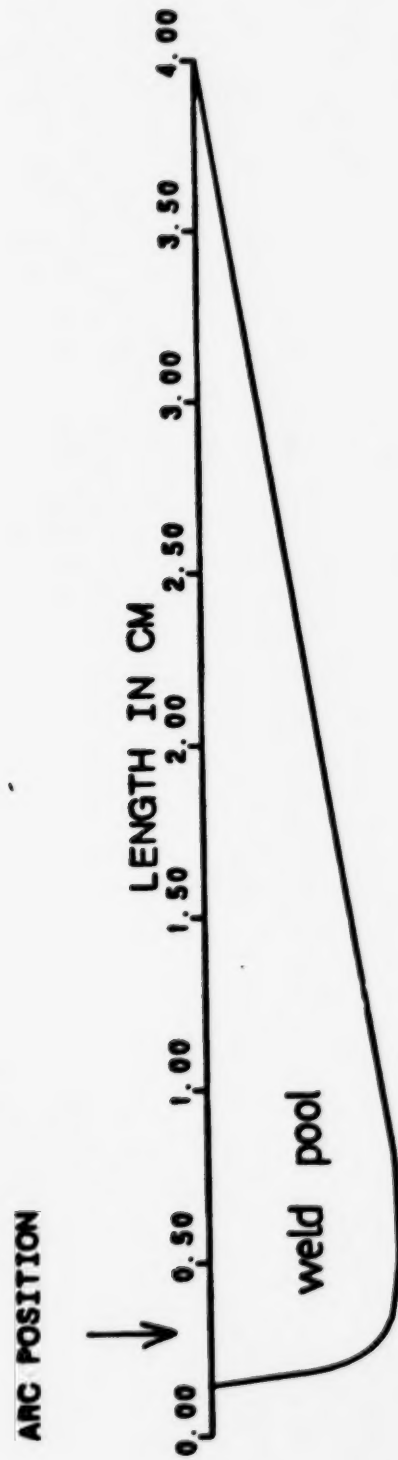


FIGURE 2.26. FINITE ELEMENT PREDICTED WELD POOL SHAPE
ALONG THE WELDING DIRECTION

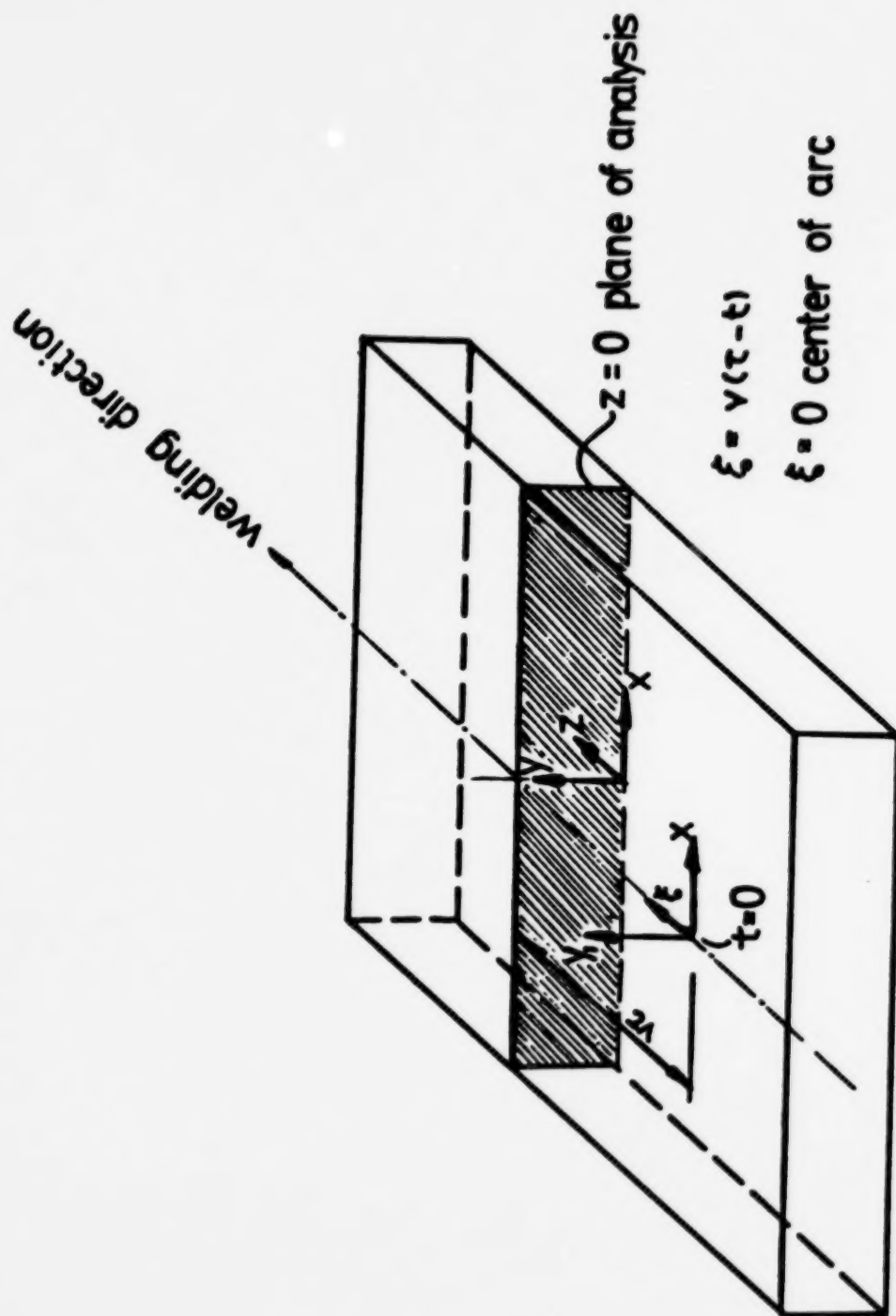


FIGURE 3.1. A PLANE SECTION NORMAL TO THE WELD FOR 2-D STRESS ANALYSIS

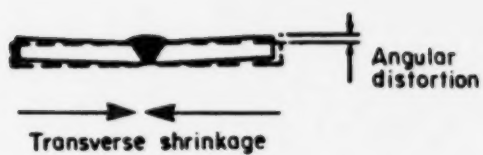


FIGURE 3.2. IN-PLANE DISTORTIONS DURING WELDING OF FLAT PLATE

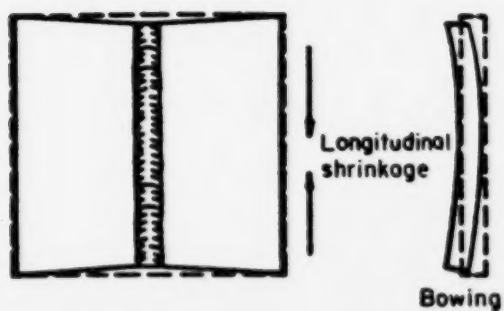


FIGURE 3.3. OUT-OF-PLANE DISTORTIONS DURING WELDING OF FLAT PLATE

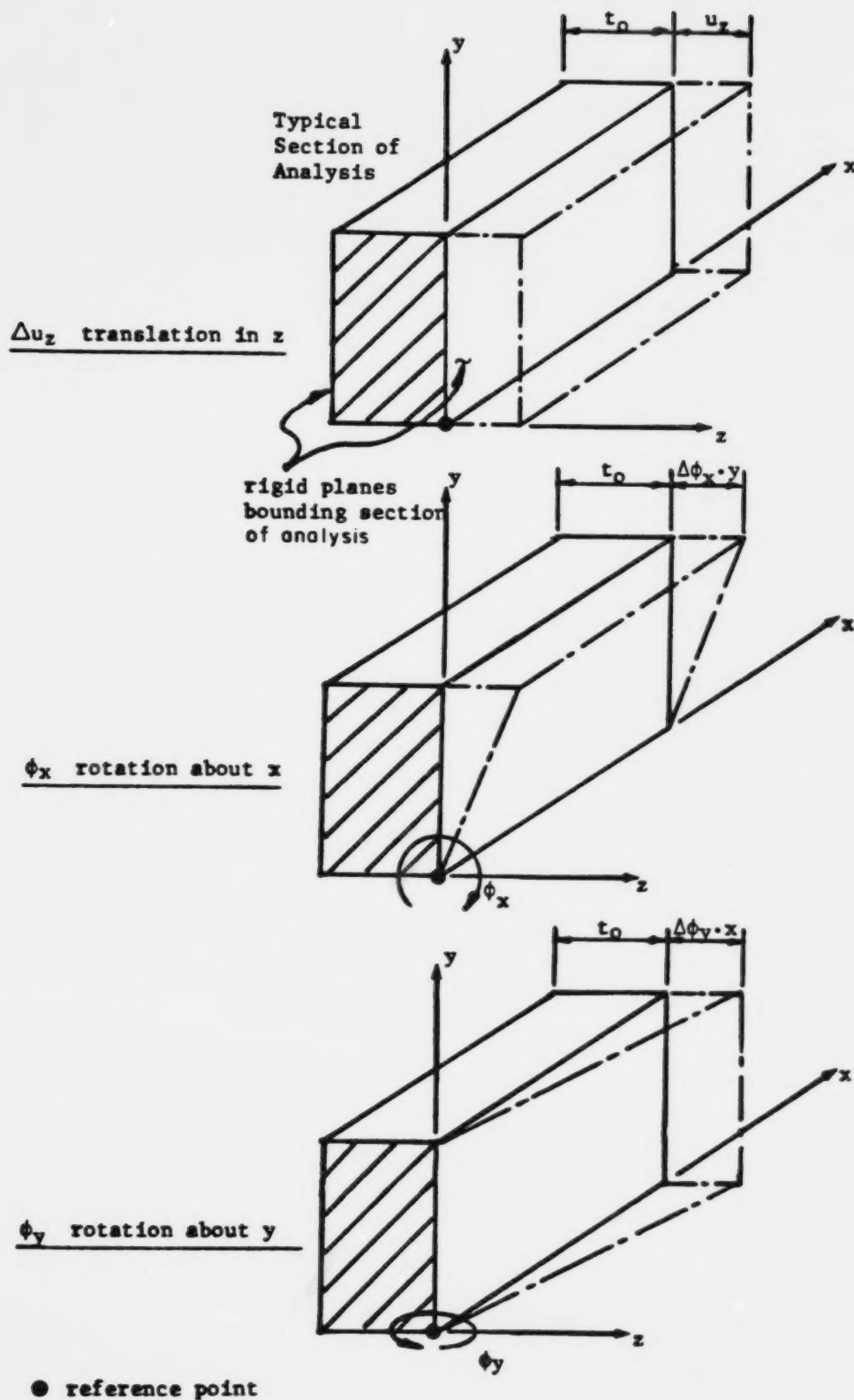


FIGURE 3.4. GENERALIZED PLANE STRAIN THEORY
- DEGREES OF FREEDOM

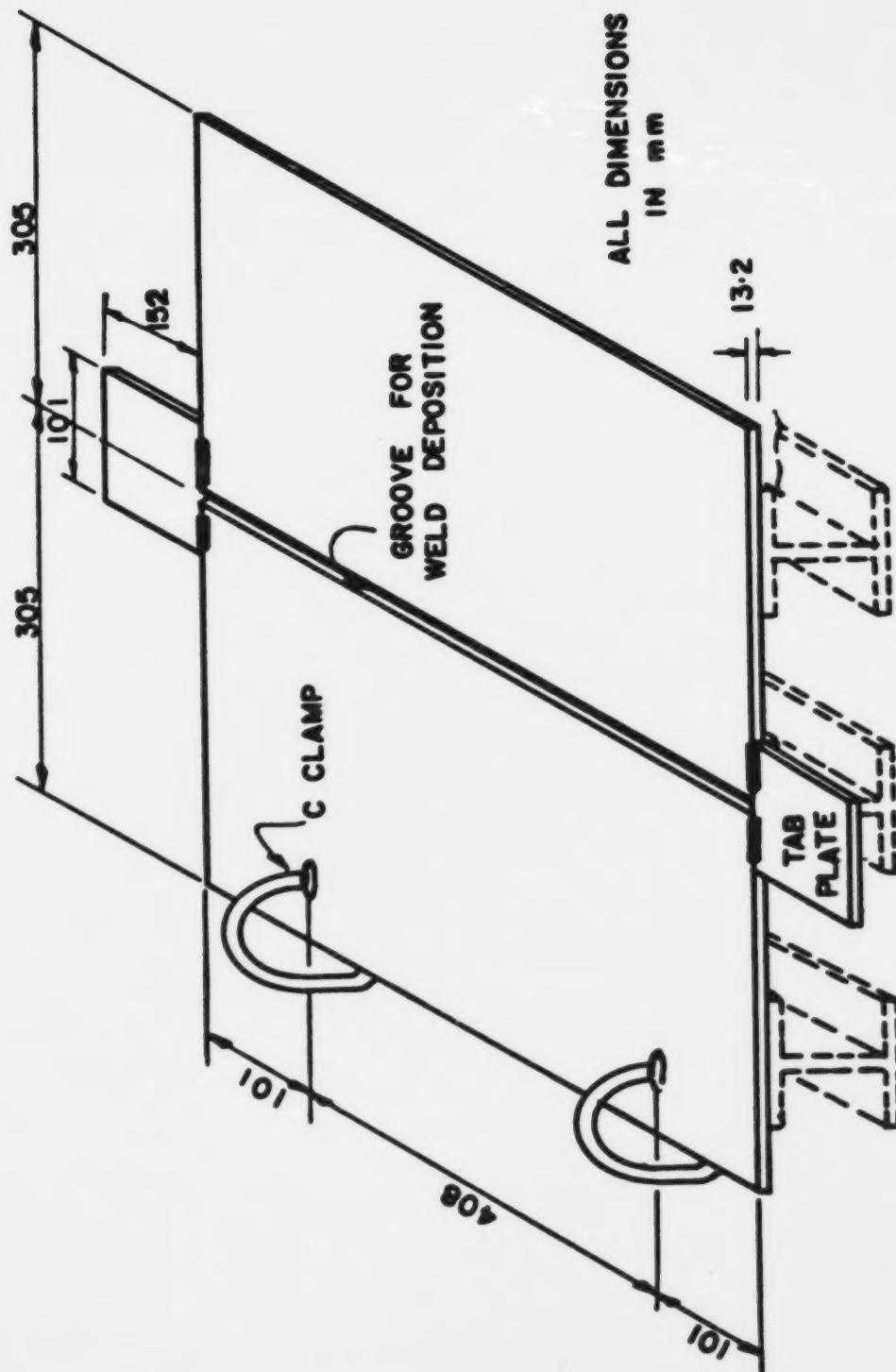


FIGURE 3.5 WELDING TEST SETUP



FIGURE 3.6 IN-PLANE KINEMATIC CONSTRAINTS

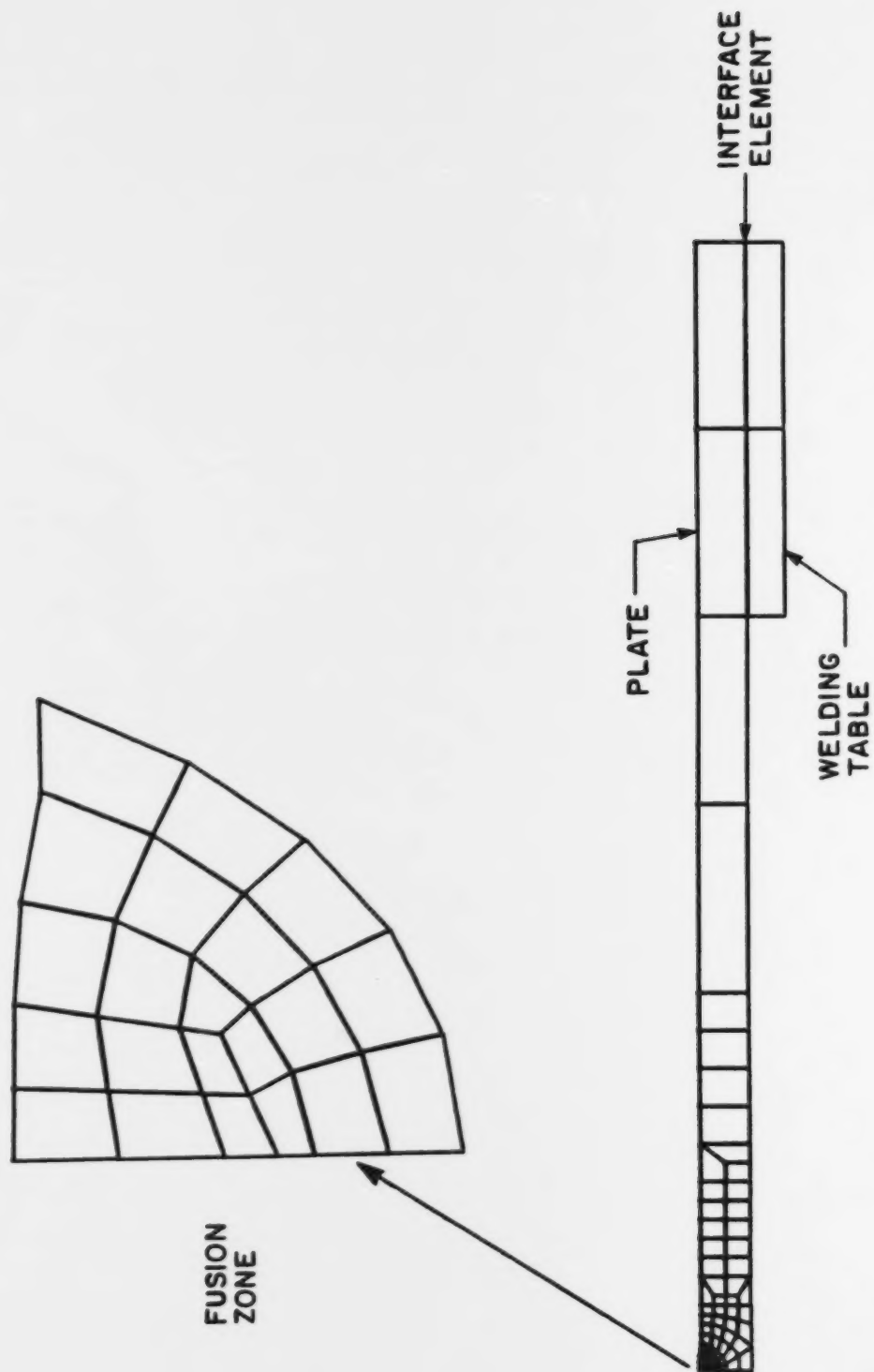


FIGURE 3.7 FINITE ELEMENT MESH FOR RESIDUAL STRESS ANALYSIS

ABAQUS VERSION 4.2-1.67

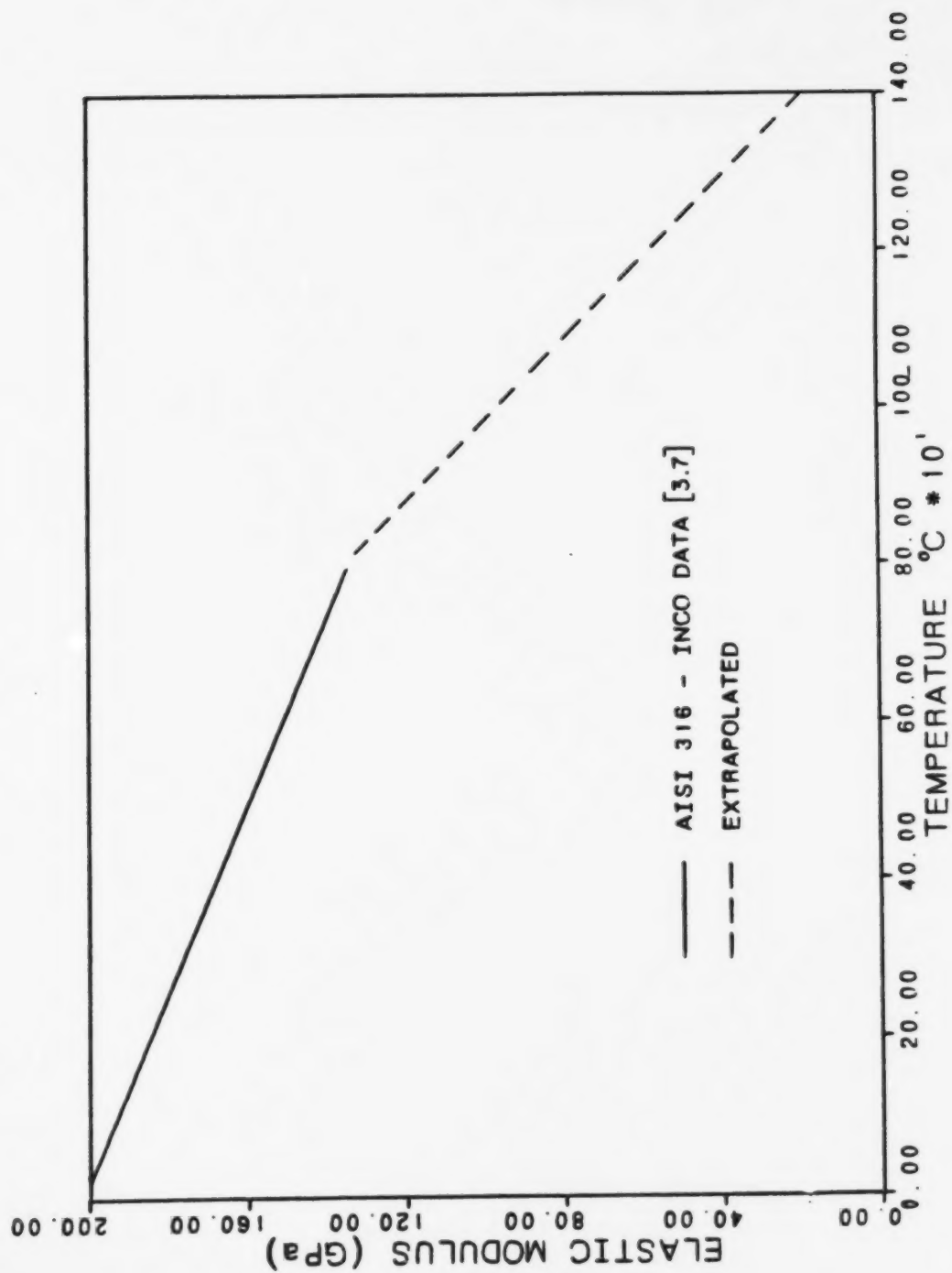


FIGURE 3.8 ELASTIC MODULUS OF AISI 316L STAINLESS STEEL

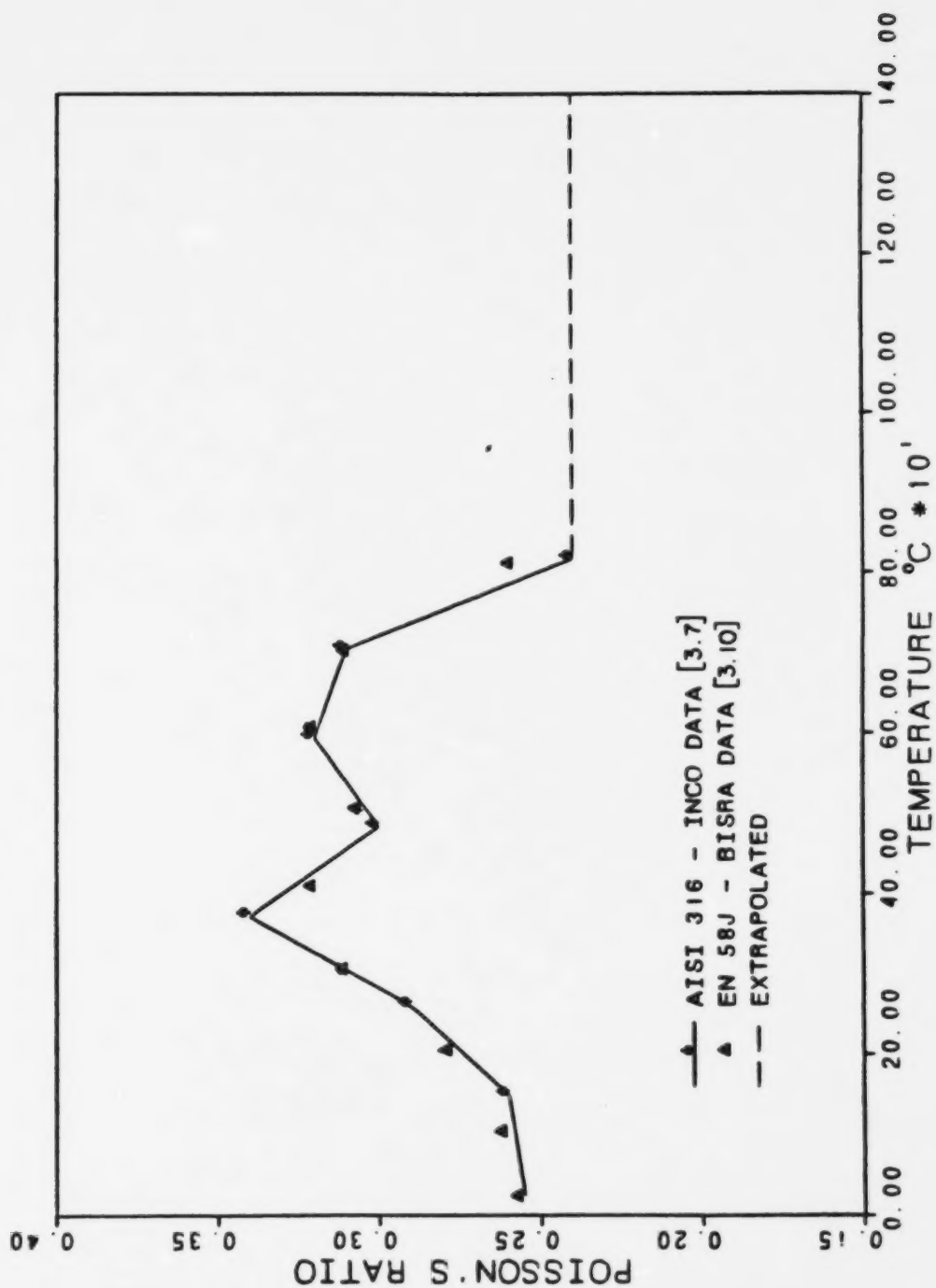


FIGURE 3.9 POISSON'S RATIO OF AISI 316L STAINLESS STEEL

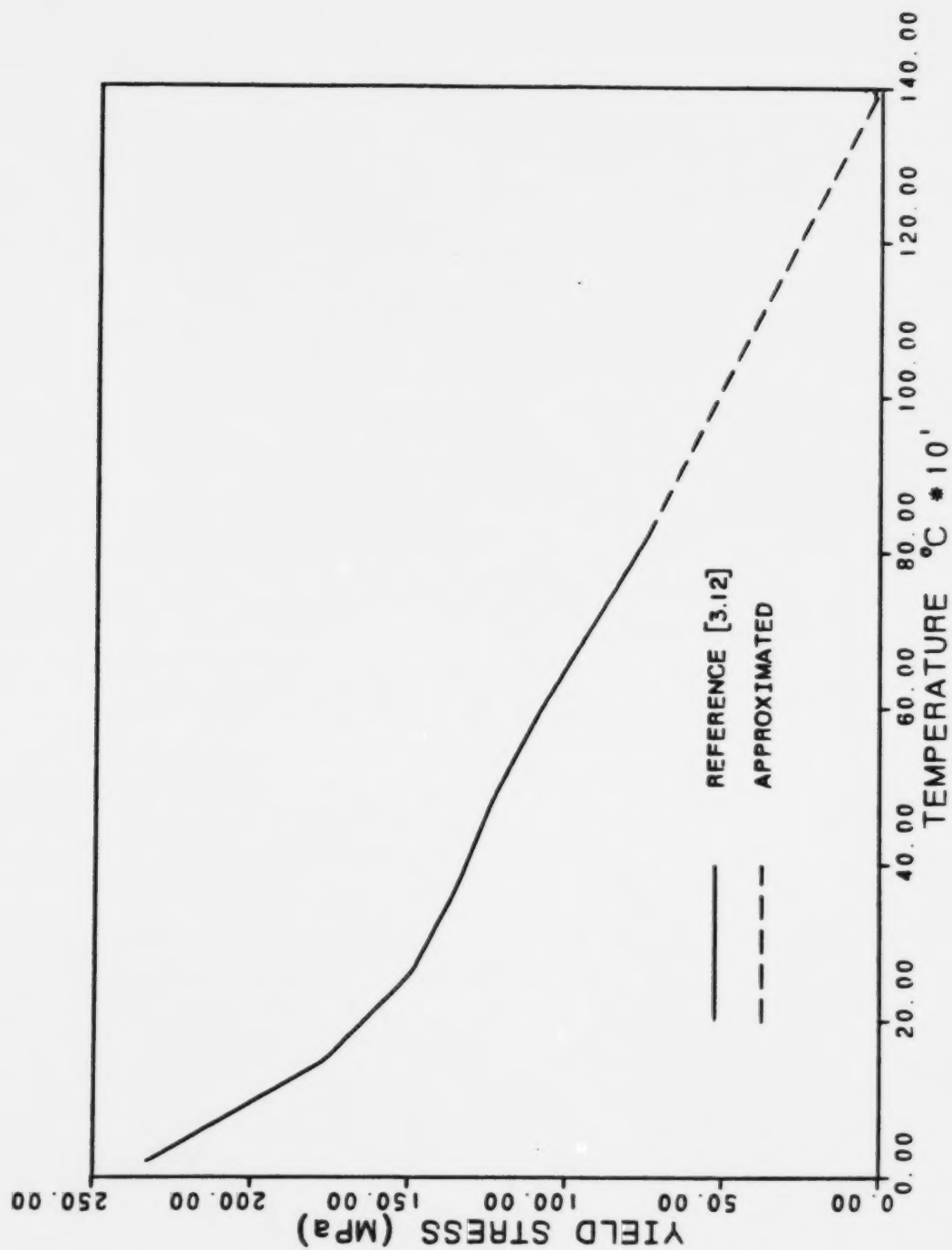


FIGURE 3.10 0.2% YIELD STRESS OF AISI 316L STAINLESS STEEL

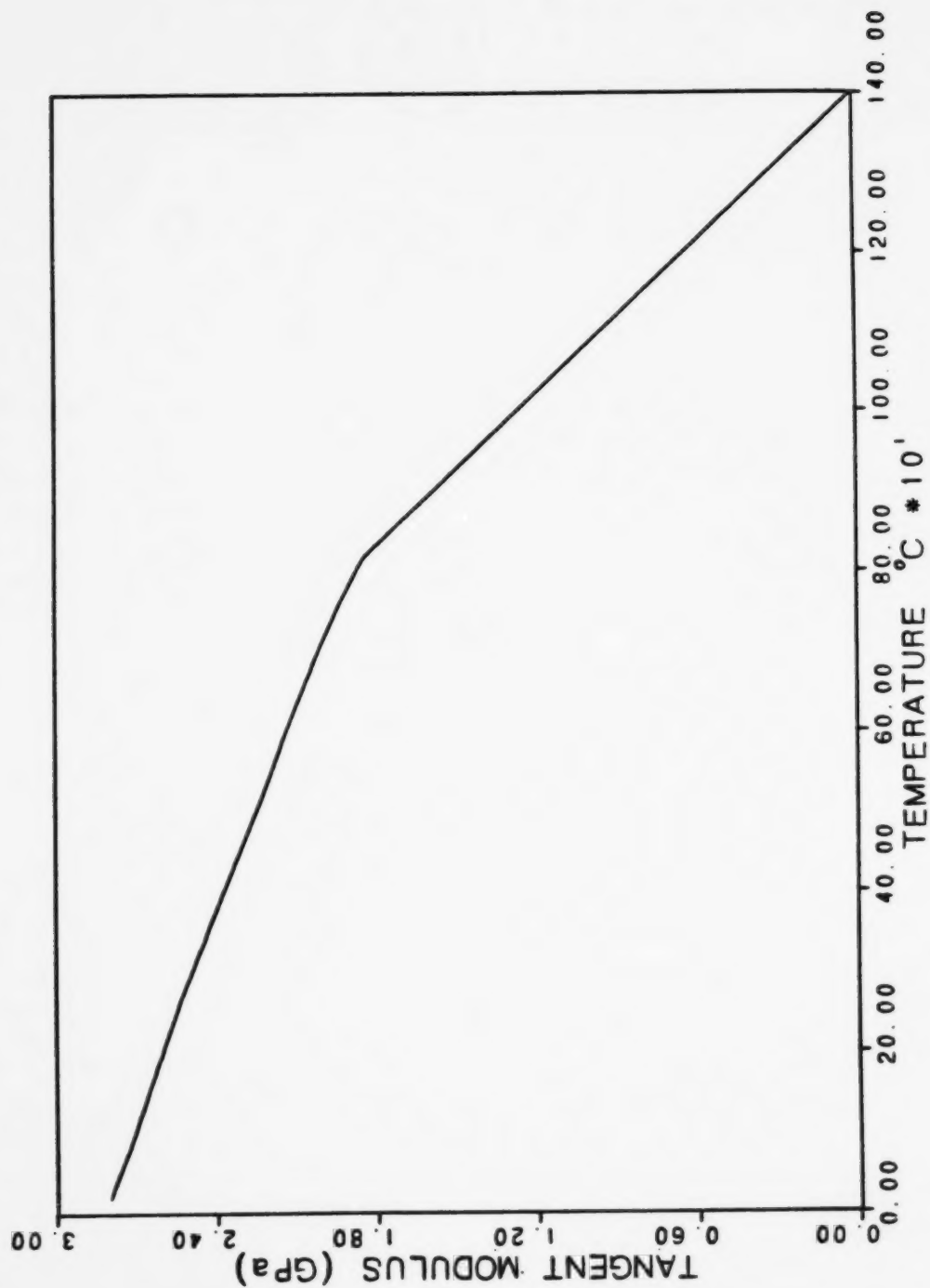


FIGURE 3.11 TANGENT MODULUS OF AISI 316L STAINLESS STEEL

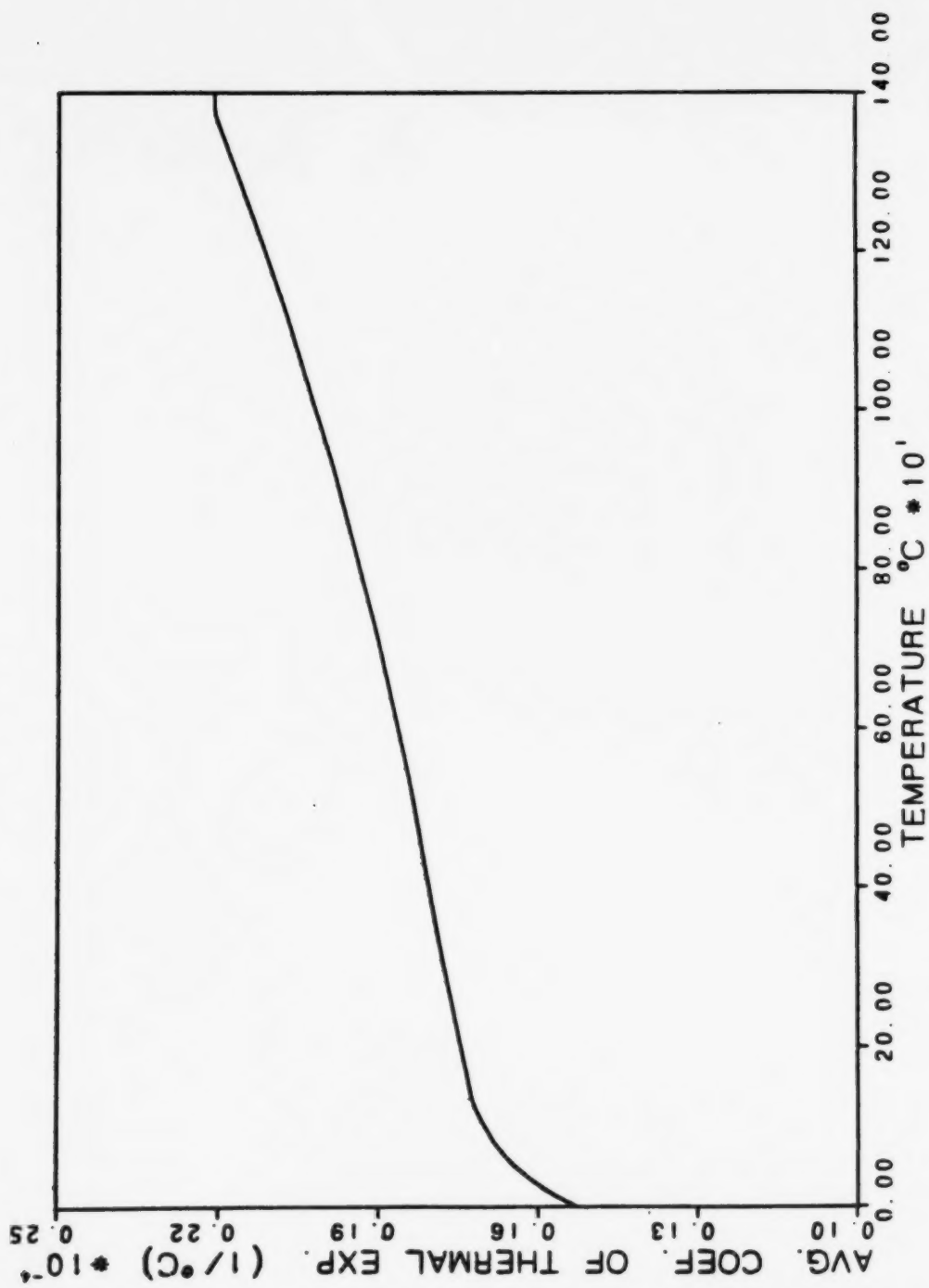


FIGURE 3.12 AVG. COEF. OF THERMAL EXP. OF AISI 316 STAINLESS STEEL

DISPL. FACTOR = 0.38E+09
 SOLID LINES - DISPLACED MESH
 DASHED LINES - ORIGINAL MESH



FIGURE 3.13 ANGULAR ROTATION OF PLATE SECTION AFTER WELDING

STEP 2 INCREMENT 1 ABAQUS VERSION 4.8-147

DISPL. $\square$
WAB. FACTOR = 0.1×10^{-6}
SOLID LINES - DISPLACED MESH
DASHED LINES - ORIGINAL MESH

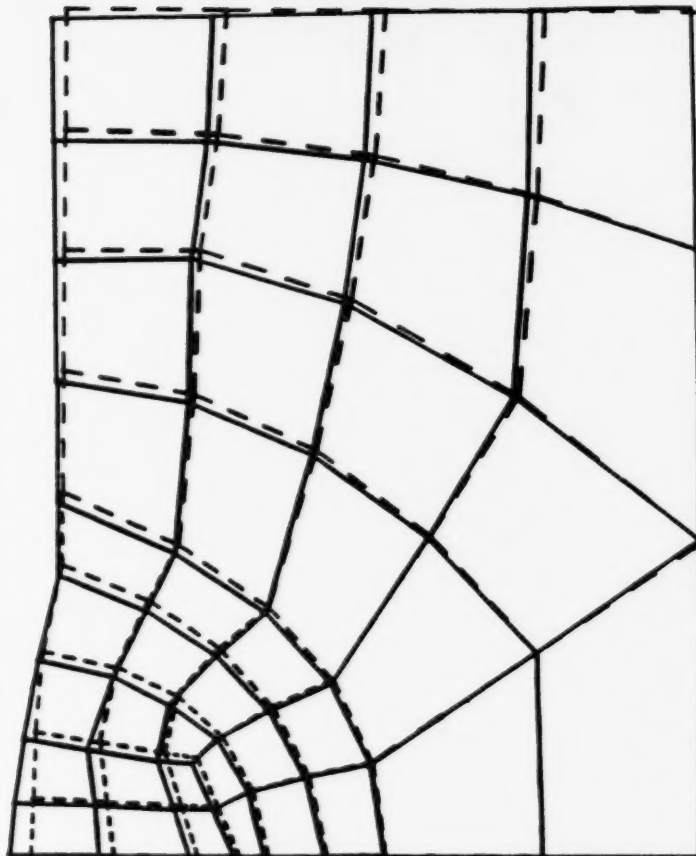


FIGURE 3.14 DISTORTED FUSION ZONE ELEMENTS
STEP 3 INCREMENT 1 ABAQUS VERSION 4.5-147

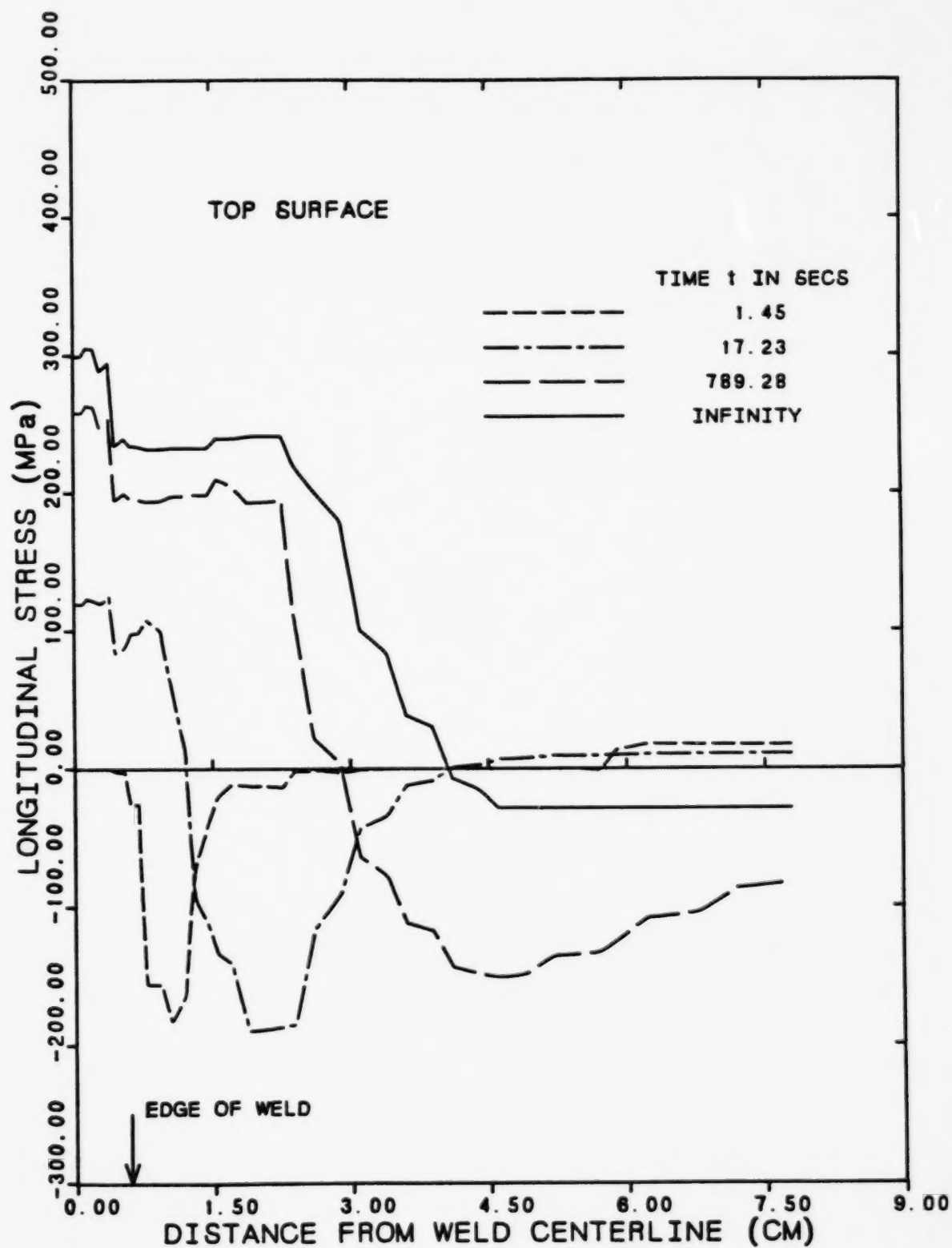


FIGURE 3.15 LONGITUDINAL STRESS DISTRIBUTION ON THE TOP SURFACE OF PLATE AT DIFFERENT TIMES

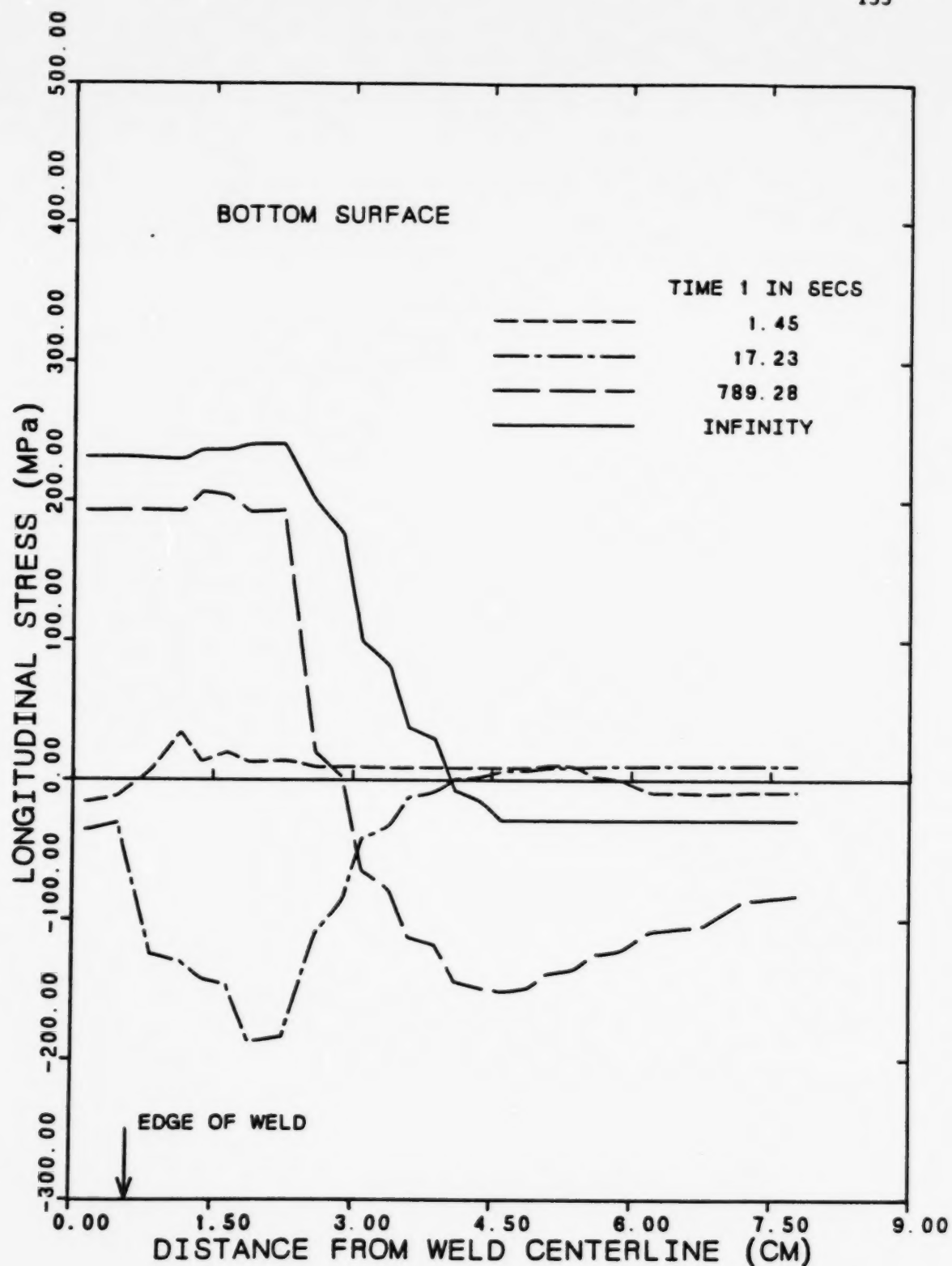


FIGURE 3.16 LONGITUDINAL STRESS DISTRIBUTION ON THE BOTTOM SURFACE OF PLATE AT DIFFERENT TIMES

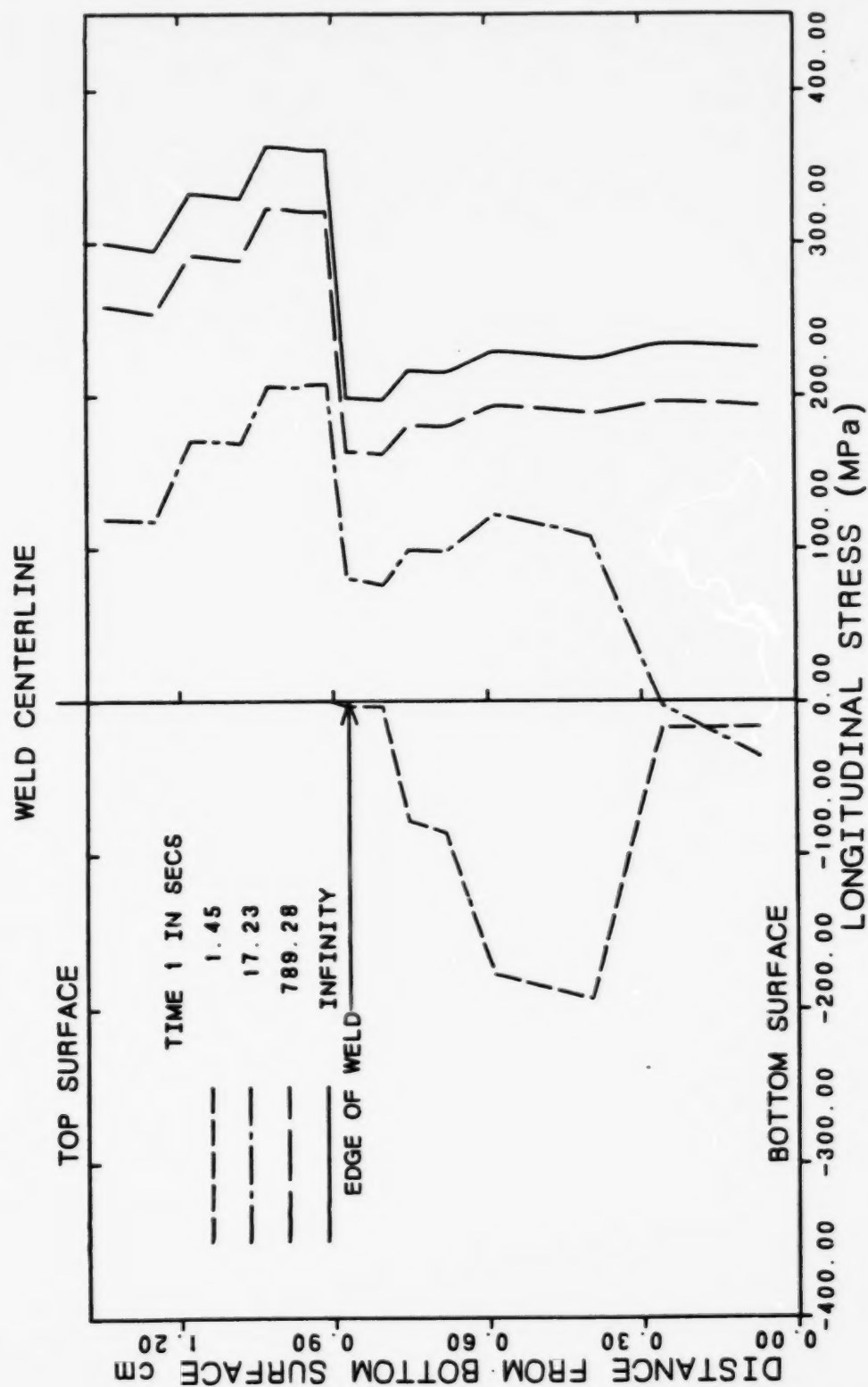


FIGURE 3.17 LONGITUDINAL STRESS DISTRIBUTION ALONG THE WELD CENTERLINE AT DIFFERENT TIMES

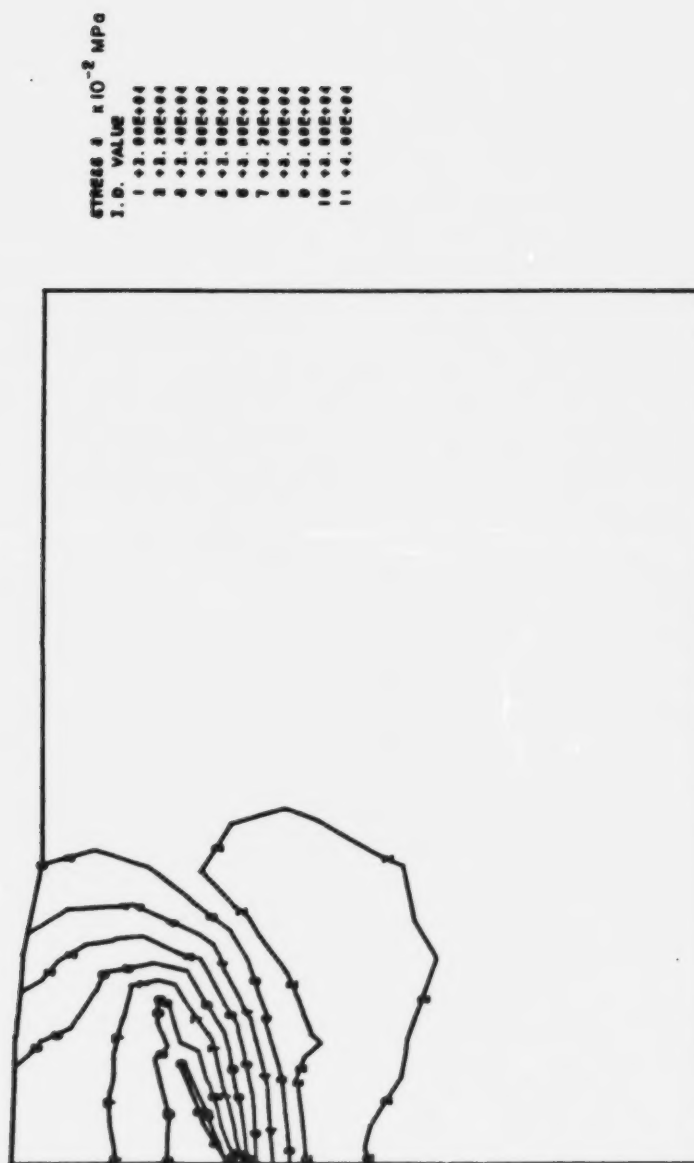


FIGURE 3.18 CONTOUR PLOT OF LONGITUDINAL RESIDUAL STRESS
STEP 3 INCREMENT 1 ABAQUS VERSION 4.3-147

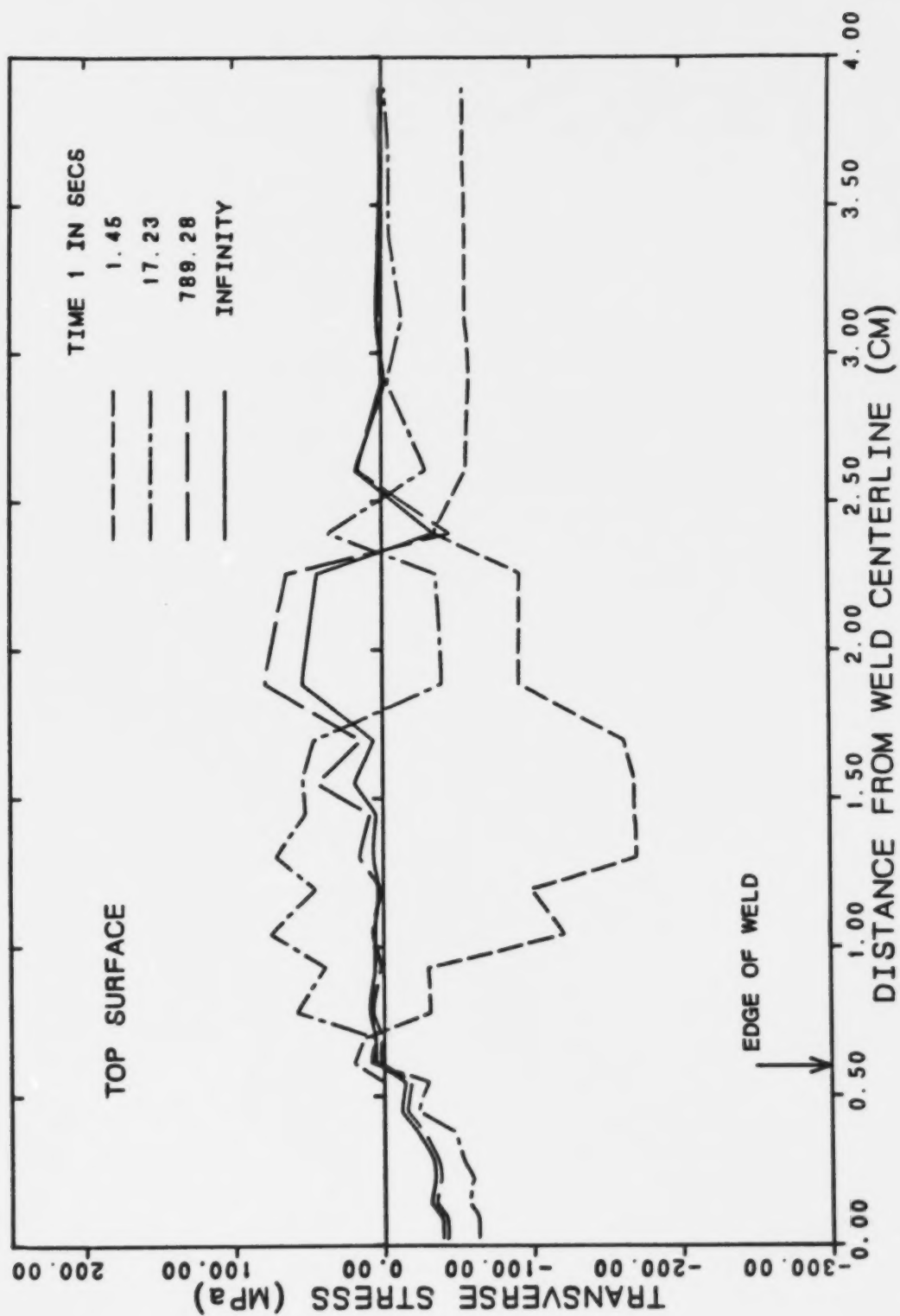


FIGURE 3.19 TRANSVERSE STRESS DISTRIBUTION ON THE TOP SURFACE OF PLATE AT DIFFERENT TIMES

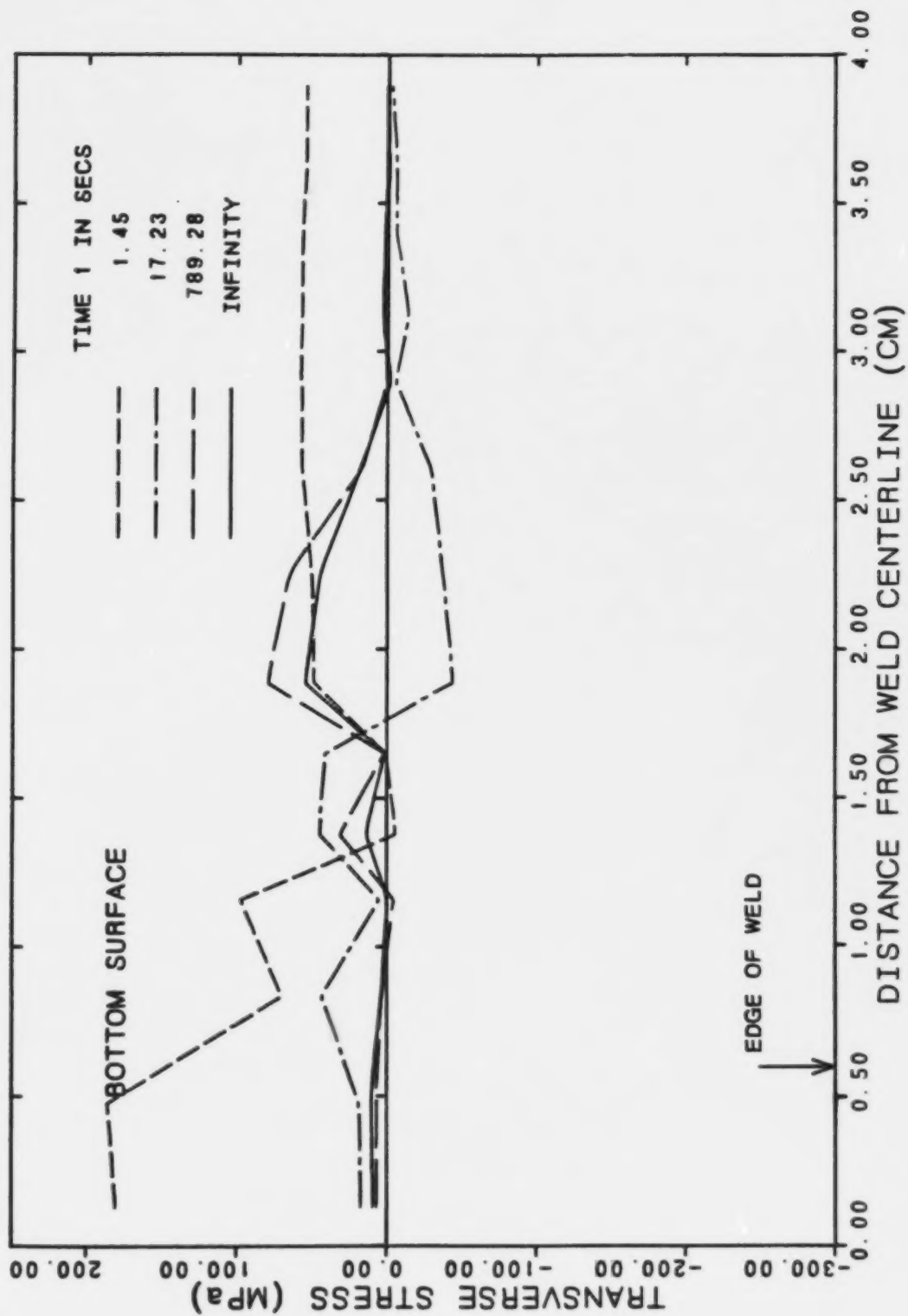


FIGURE 3.20 TRANSVERSE STRESS DISTRIBUTION ON THE BOTTOM SURFACE OF PLATE AT DIFFERENT TIMES

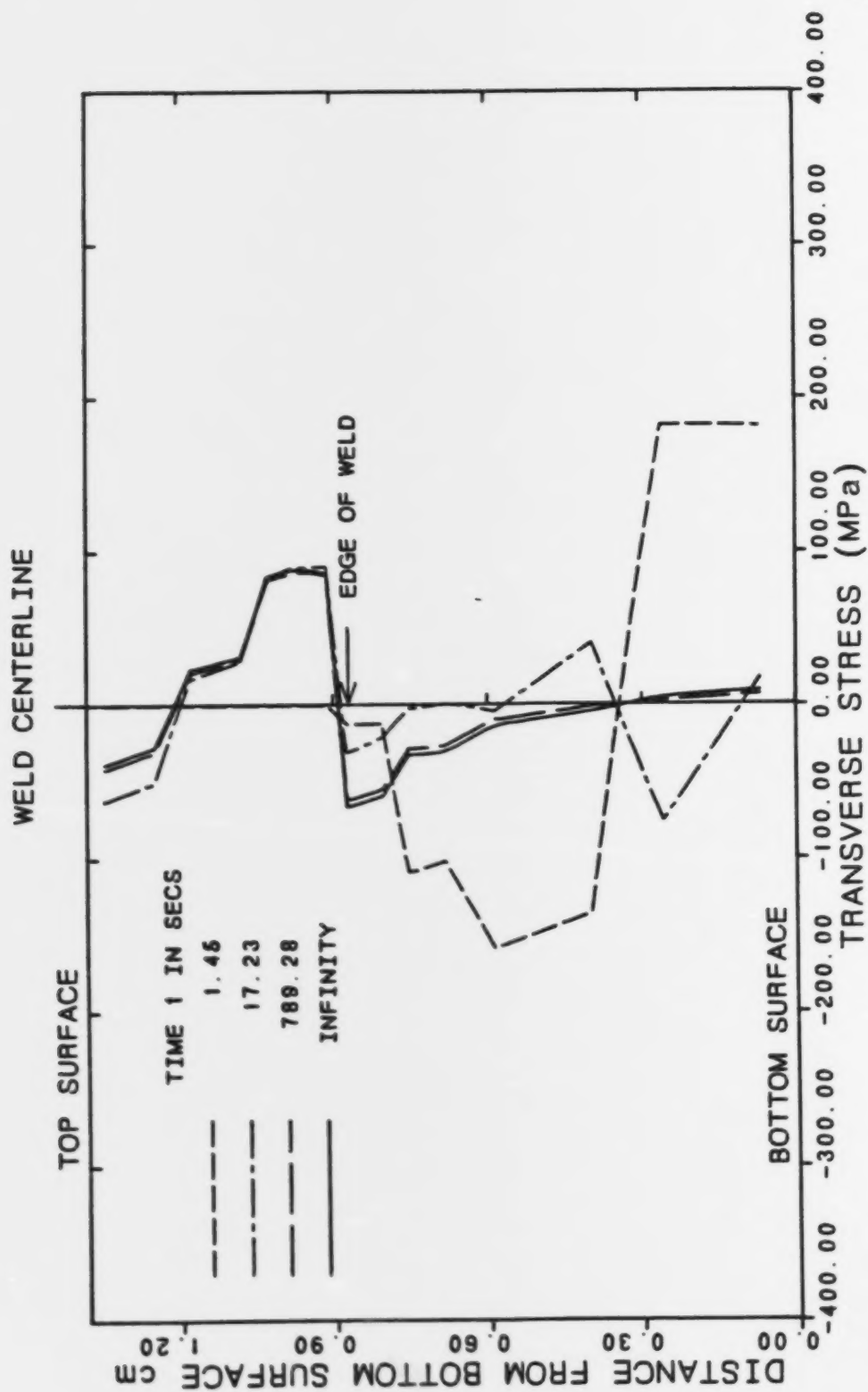


FIGURE 3.21 TRANSVERSE STRESS DISTRIBUTION ALONG THE WELD CENTERLINE AT DIFFERENT TIMES

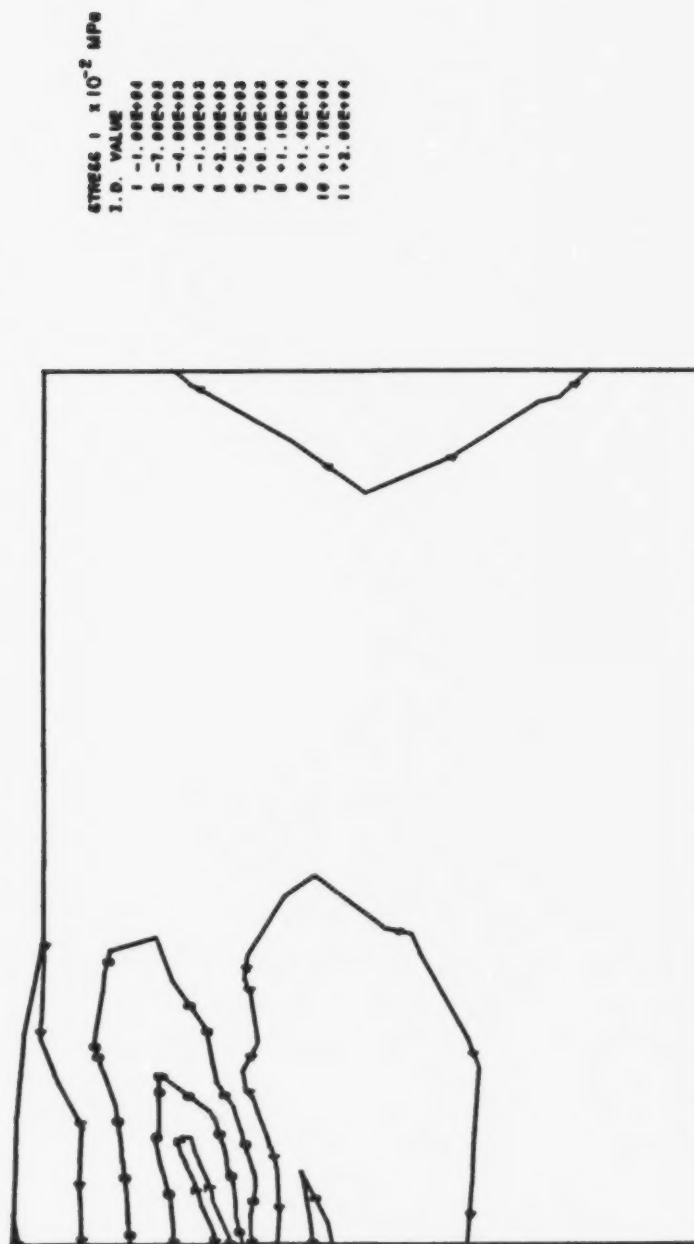


FIGURE 3.22 CONTOUR PLOT OF TRANSVERSE RESIDUAL STRESS
 STEP 2 INCREMENT 1 ABAQUS VERSION 4.5-147

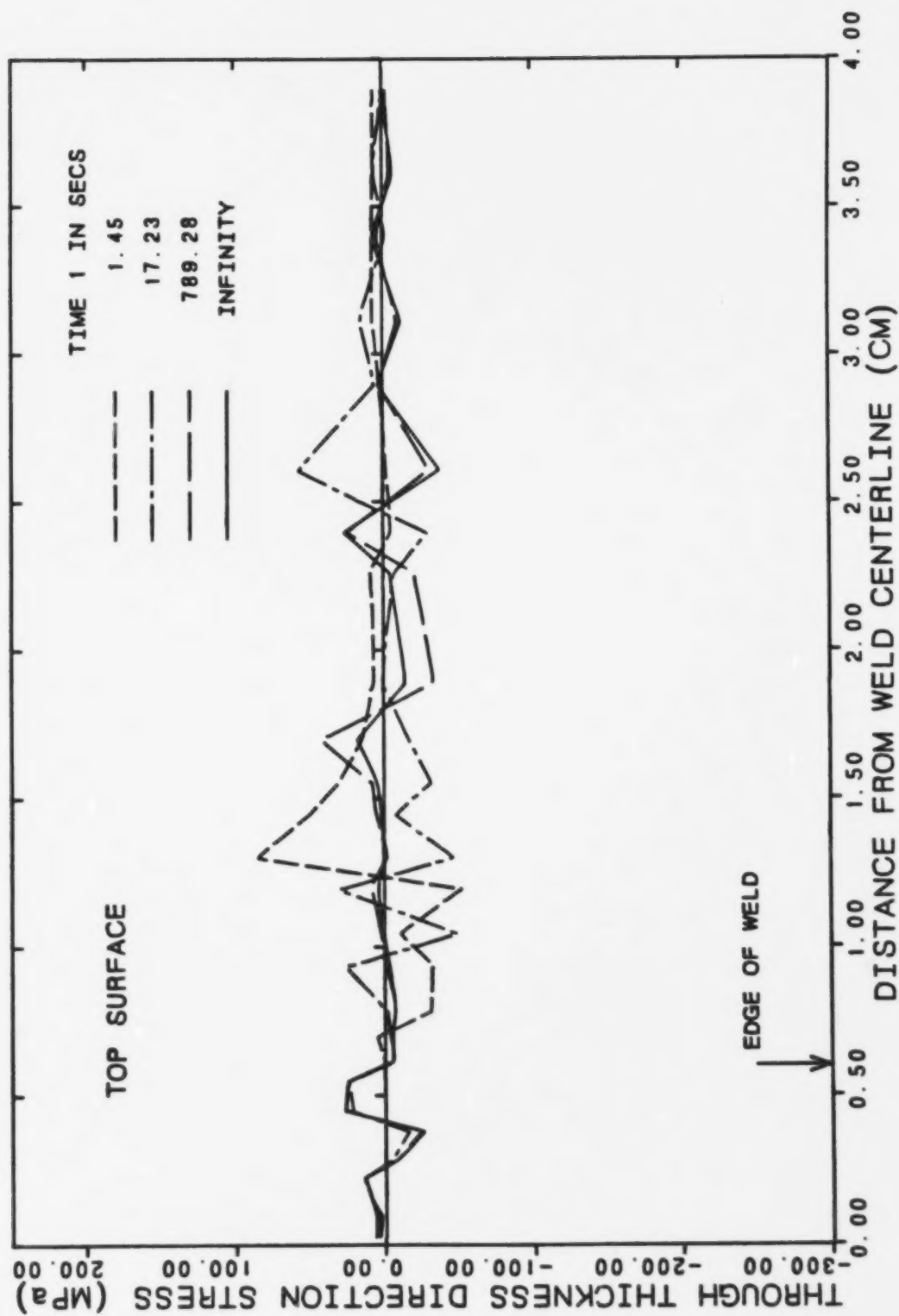


FIGURE 3.23 THROUGH THICKNESS DIRECTION STRESS DISTRIBUTION ON THE TOP SURFACE OF PLATE AT DIFFERENT TIMES

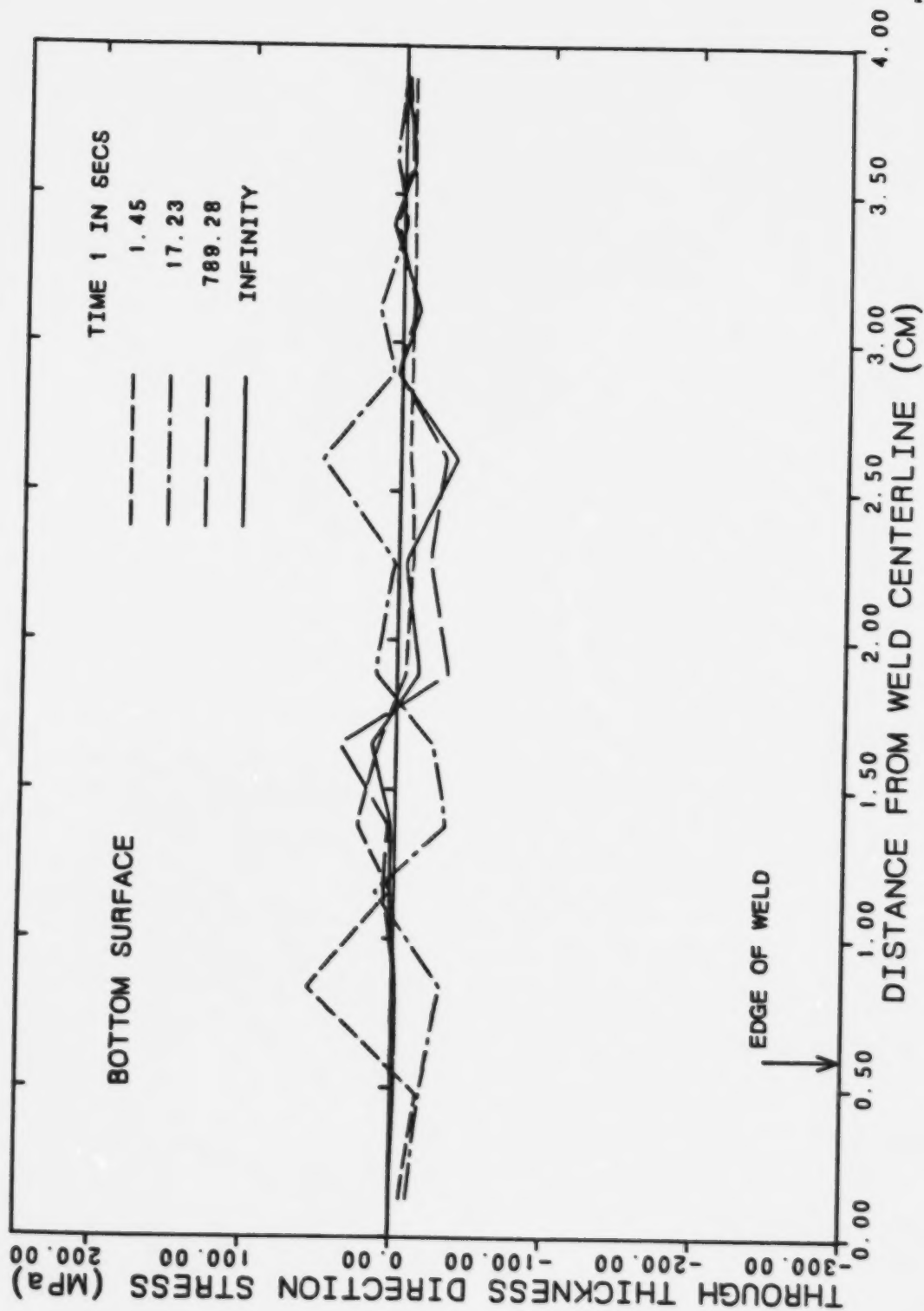


FIGURE 3.24 THROUGH THICKNESS DIRECTION STRESS DISTRIBUTION ON THE BOTTOM SURFACE OF PLATE AT DIFFERENT TIMES

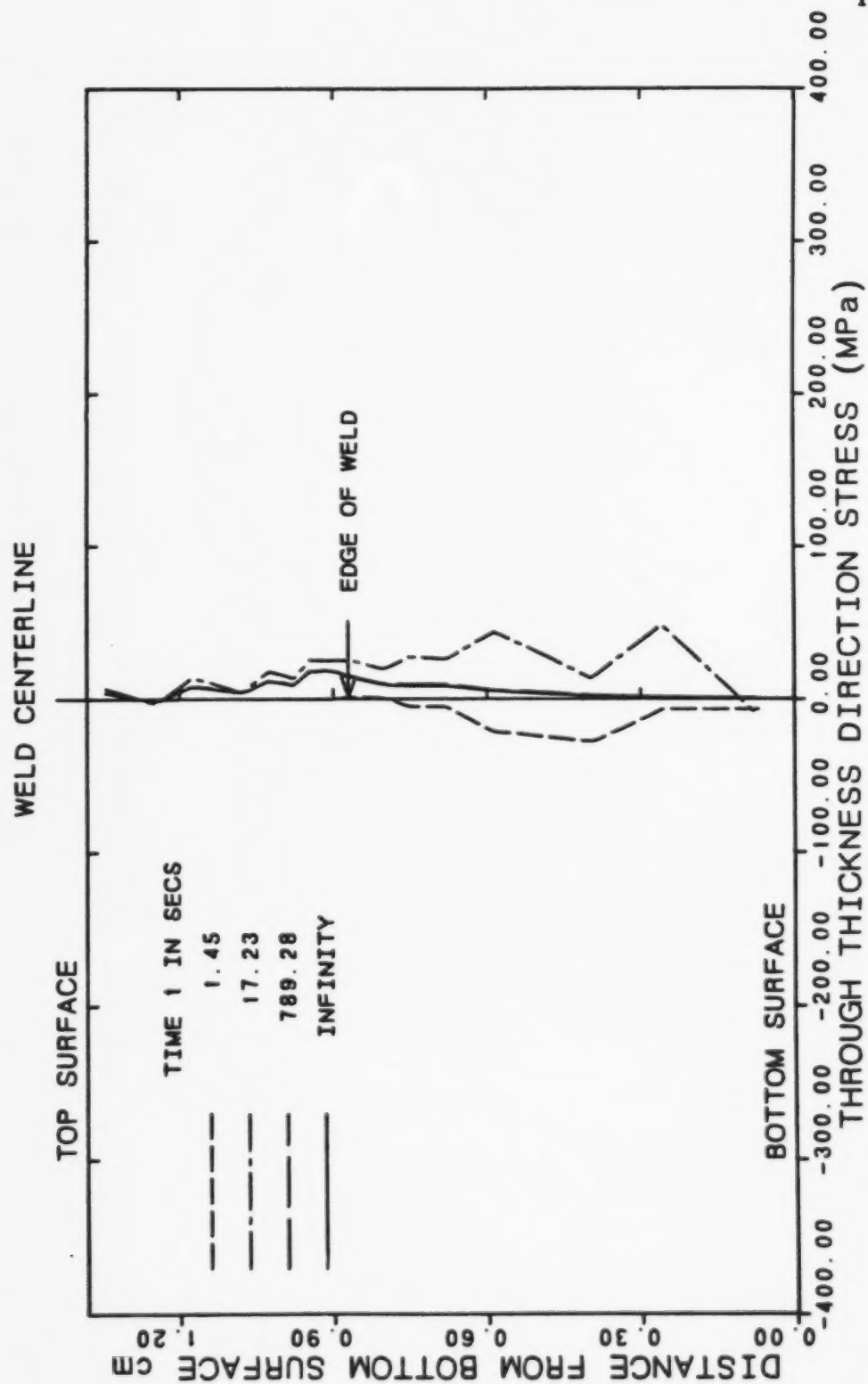


FIGURE 3.25 THROUGH THICKNESS DIRECTION STRESS DISTRIBUTION
ALONG THE WELD CENTERLINE AT DIFFERENT TIMES

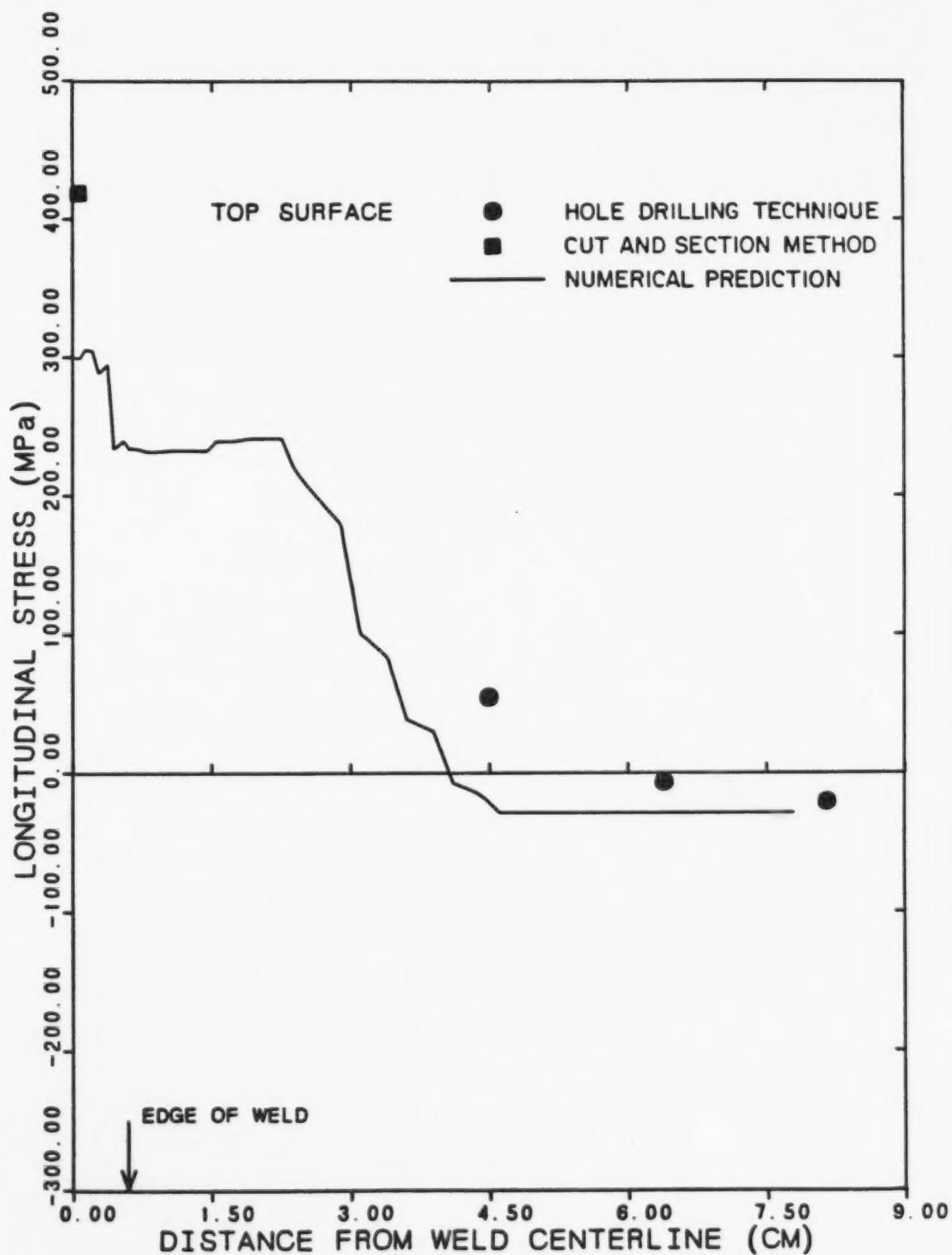


FIGURE 3.26 LONGITUDINAL RESIDUAL STRESS ON TOP SURFACE WITH EXPERIMENTAL RESULTS

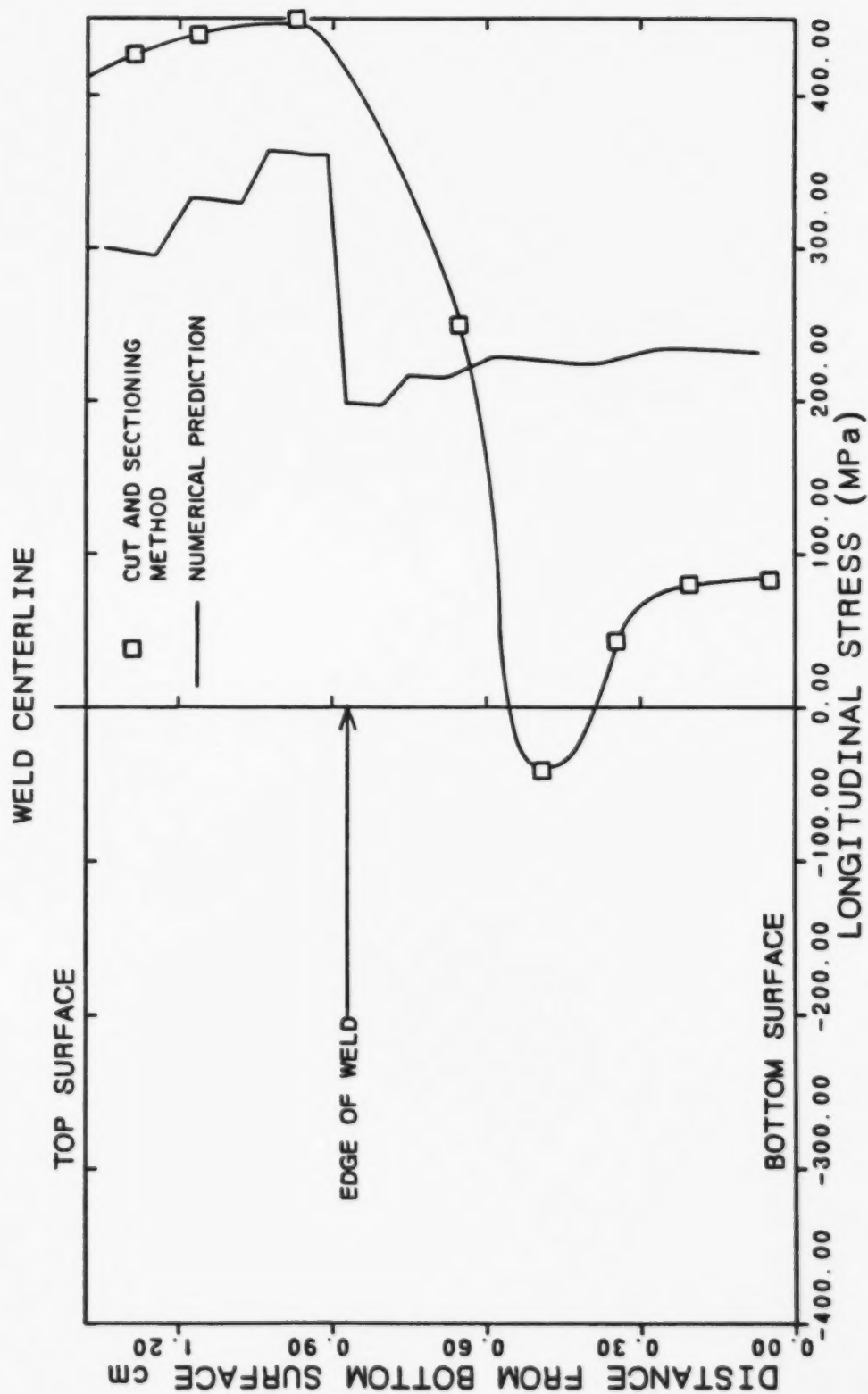


FIGURE 3.27 LONGITUDINAL RESIDUAL STRESS DISTRIBUTION THROUGH THICKNESS ALONG THE WELD CENTERLINE AND EXPERIMENTAL RESULTS

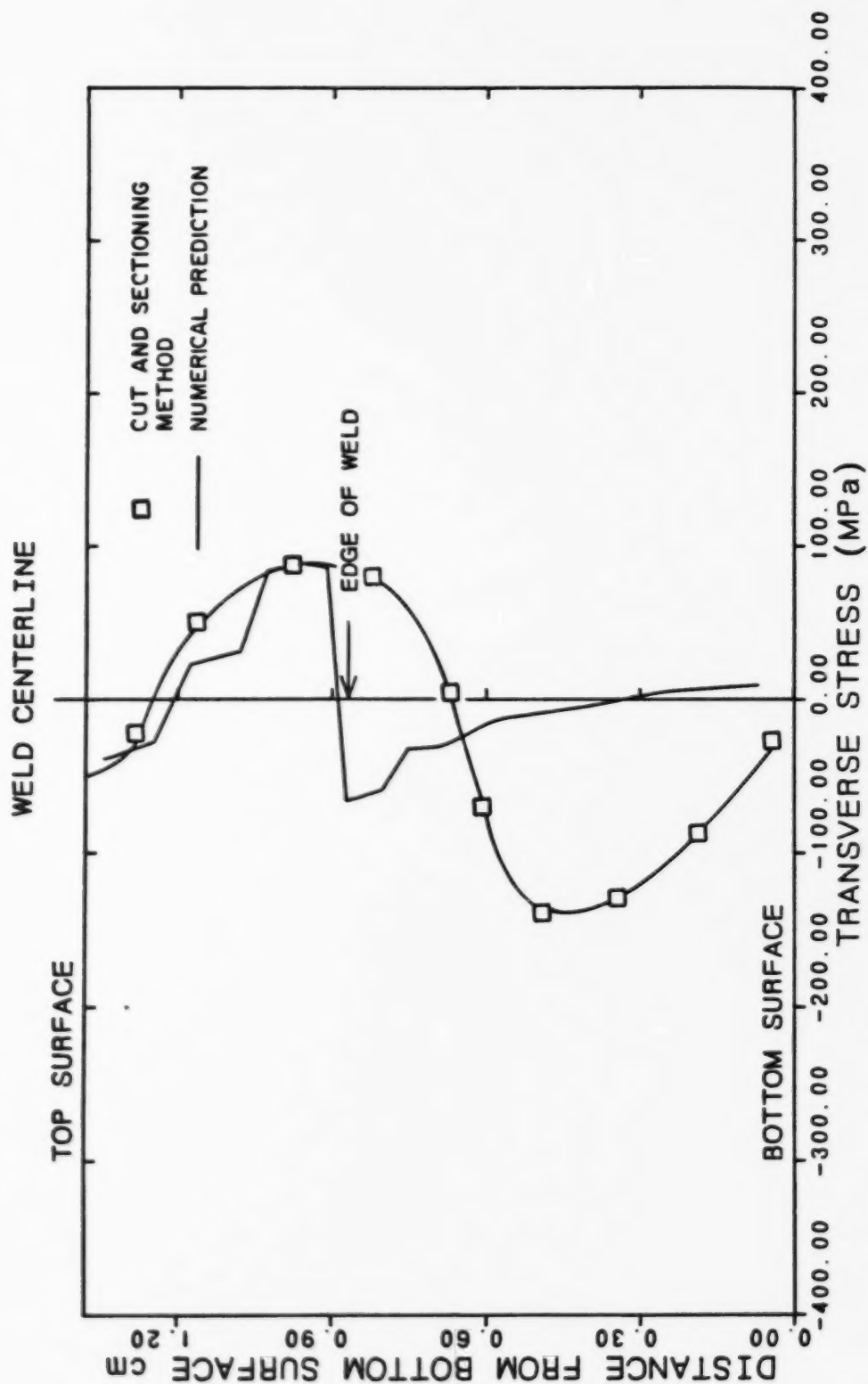


FIGURE 3.28 TRANSVERSE RESIDUAL STRESS DISTRIBUTION THROUGH THE THICKNESS ALONG THE WELD CENTERLINE AND EXPERIMENTAL RESULTS

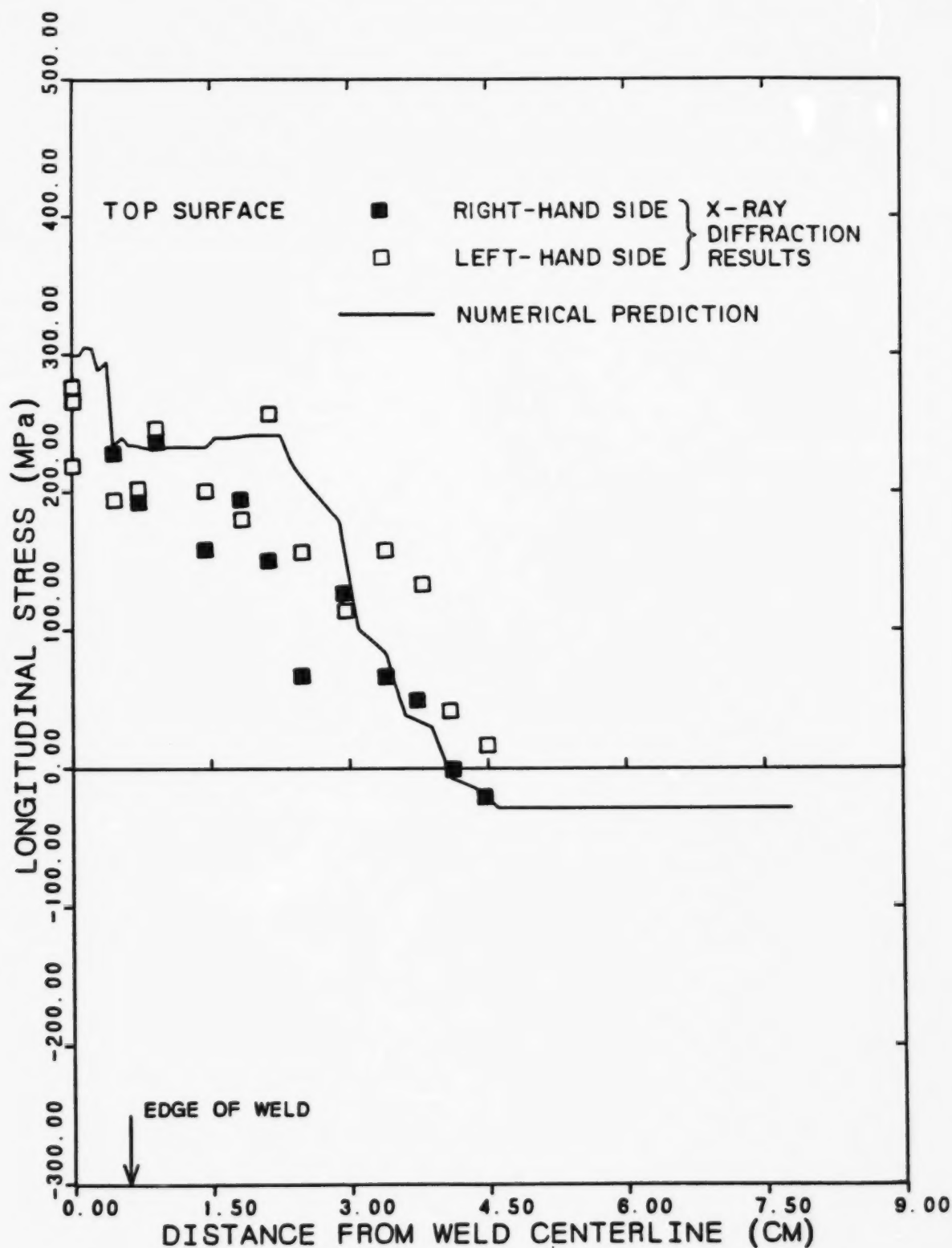


FIGURE 3.29 LONGITUDINAL RESIDUAL STRESS DISTRIBUTION ON THE TOP SURFACE WITH X-RAY DIFFRACTION RESULTS

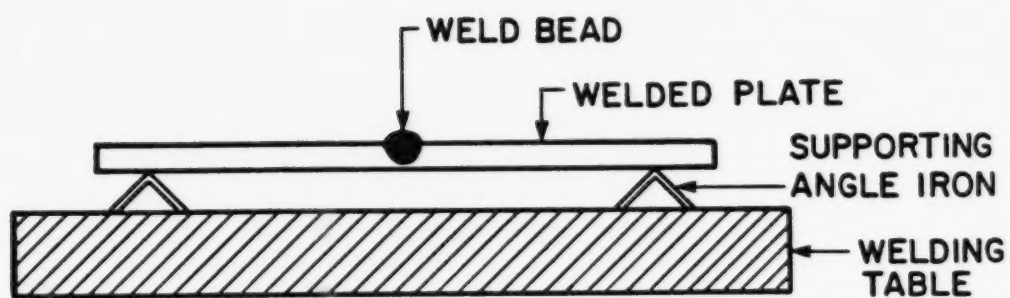


FIGURE 3.30 WELDING SETUP - SIMPLY SUPPORTED PLATE

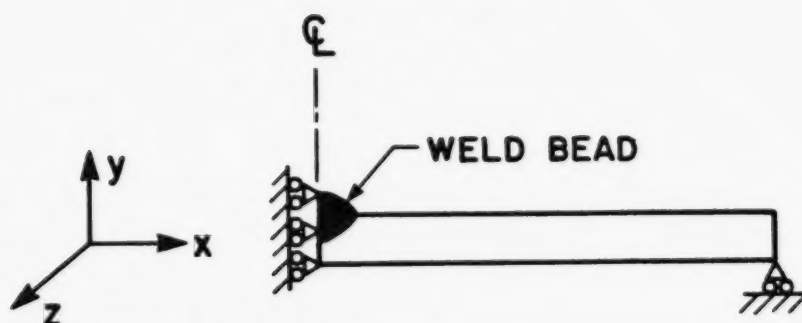


FIGURE 3.31 IN-PLANE KINEMATIC CONSTRAINTS -
SIMPLY SUPPORTED PLATE

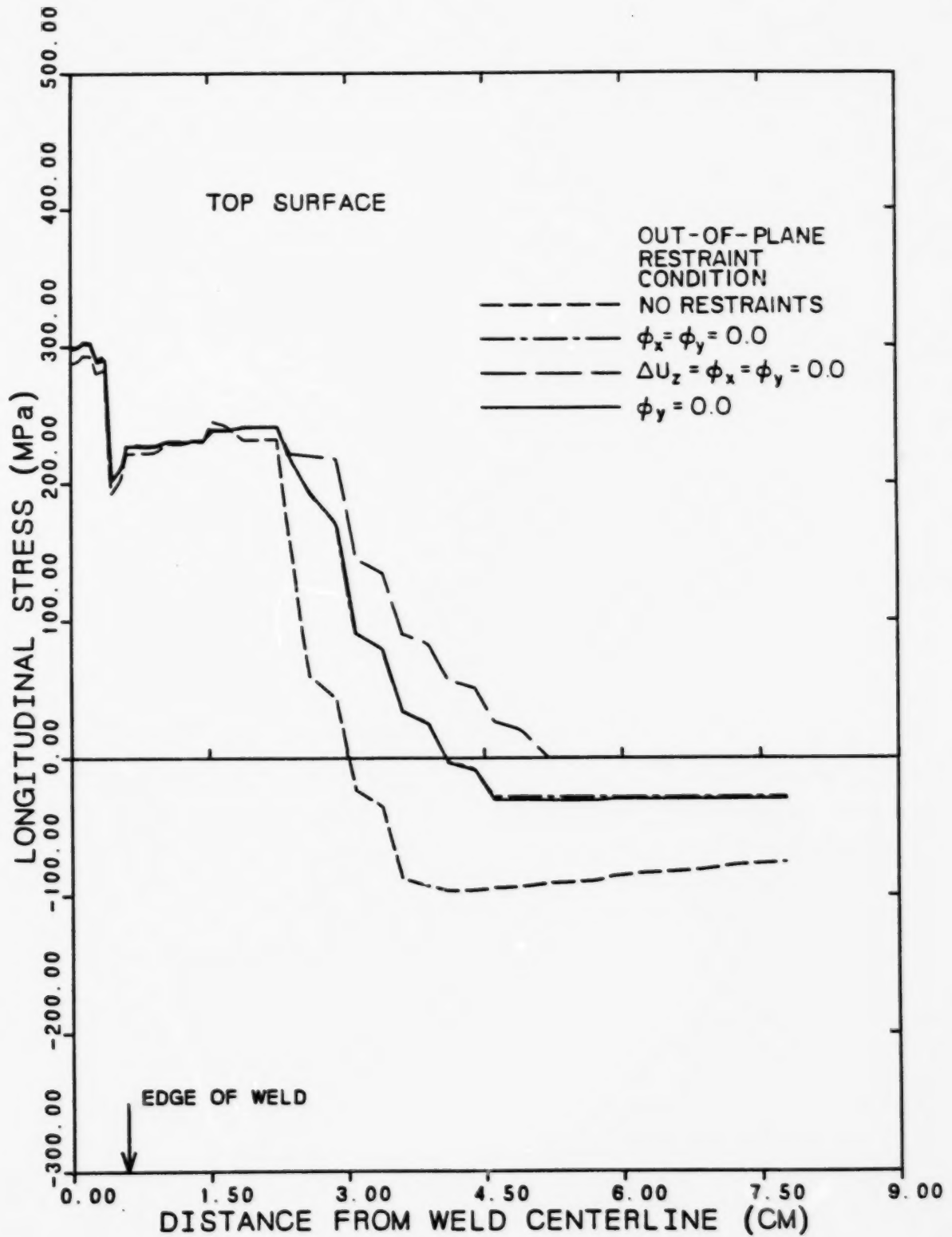


FIGURE 3.32 LONGITUDINAL RESIDUAL STRESS DISTRIBUTION ON THE TOP SURFACE - SIMPLY SUPPORTED PLATE

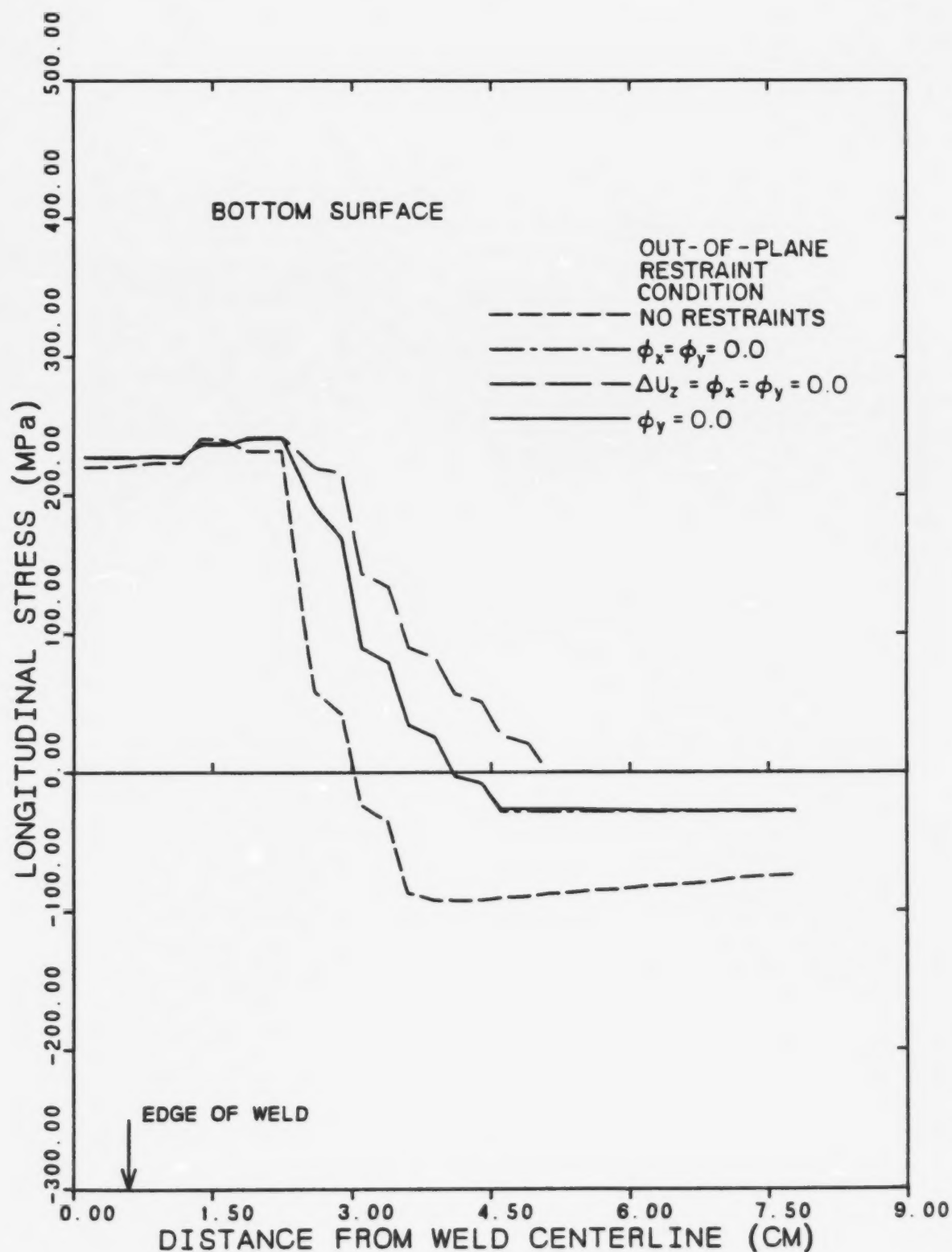


FIGURE 3.33 LONGITUDINAL RESIDUAL STRESS DISTRIBUTION ON THE BOTTOM SURFACE—SIMPLY SUPPORTED PLATE

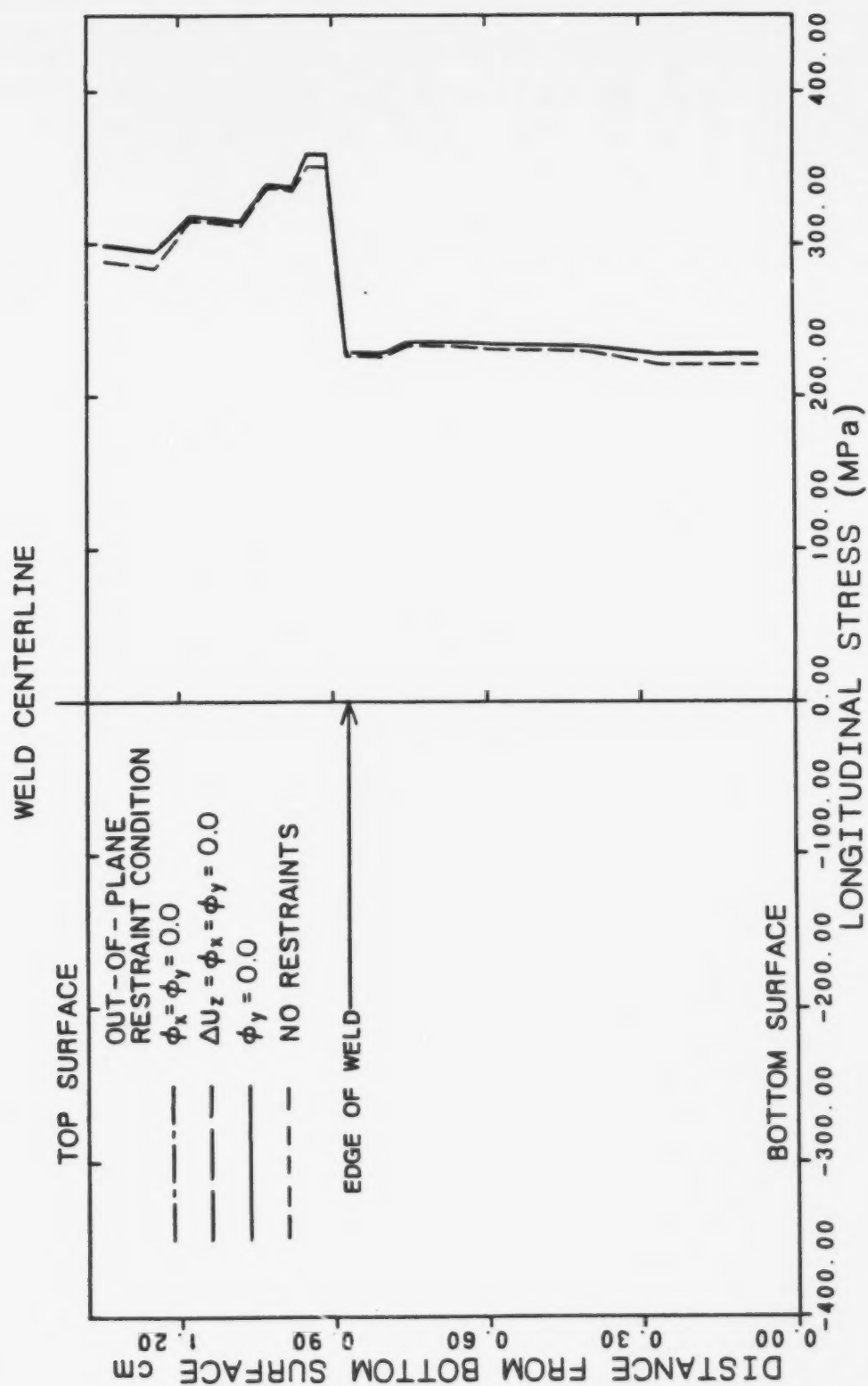


FIGURE 3.34 LONGITUDINAL RESIDUAL STRESS DISTRIBUTION ALONG THE WELD CENTERLINE
- SIMPLY SUPPORTED PLATE

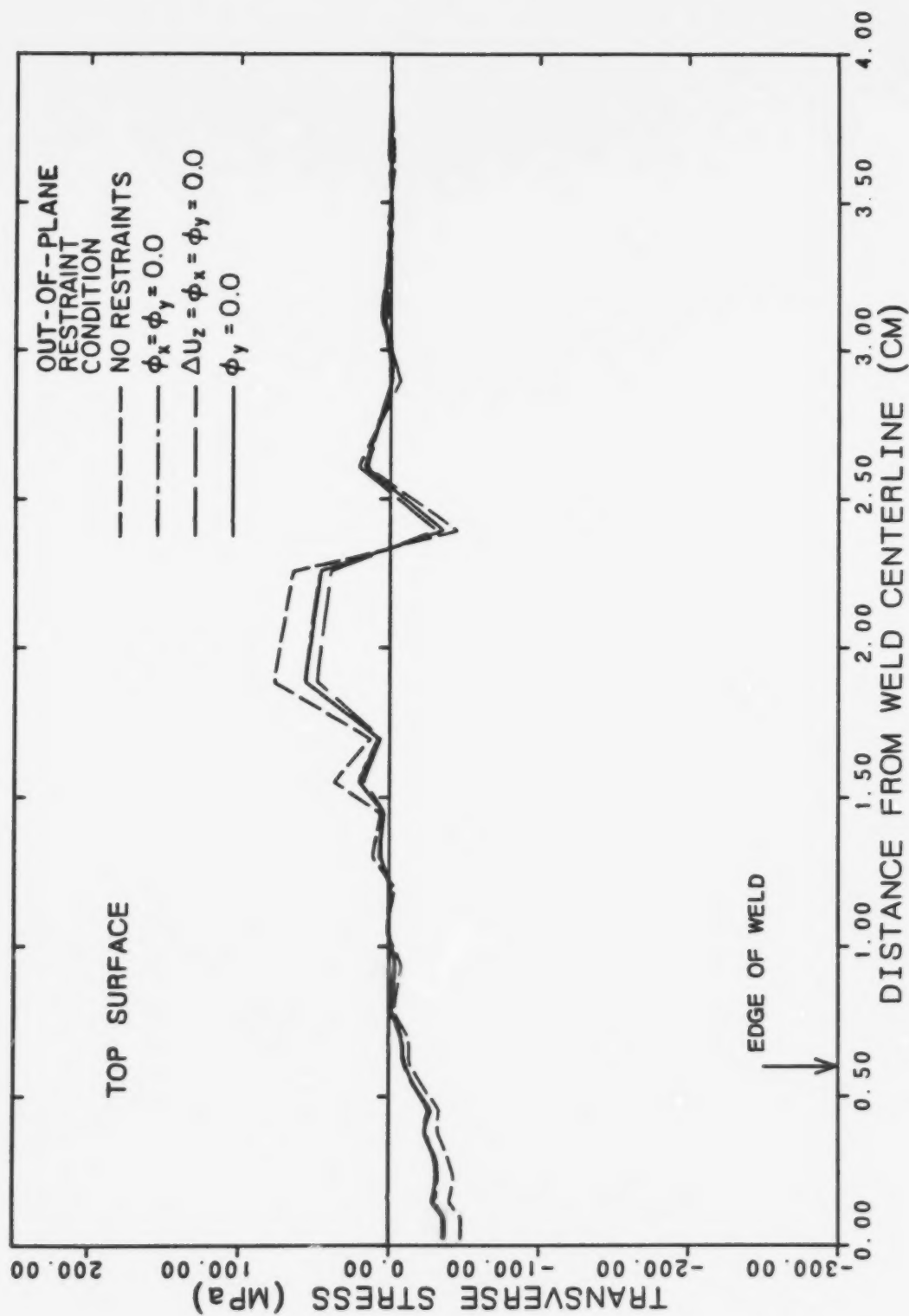


FIGURE 3.35 TRANSVERSE RESIDUAL STRESS DISTRIBUTION ON THE TOP SURFACE —
SIMPLY SUPPORTED PLATE

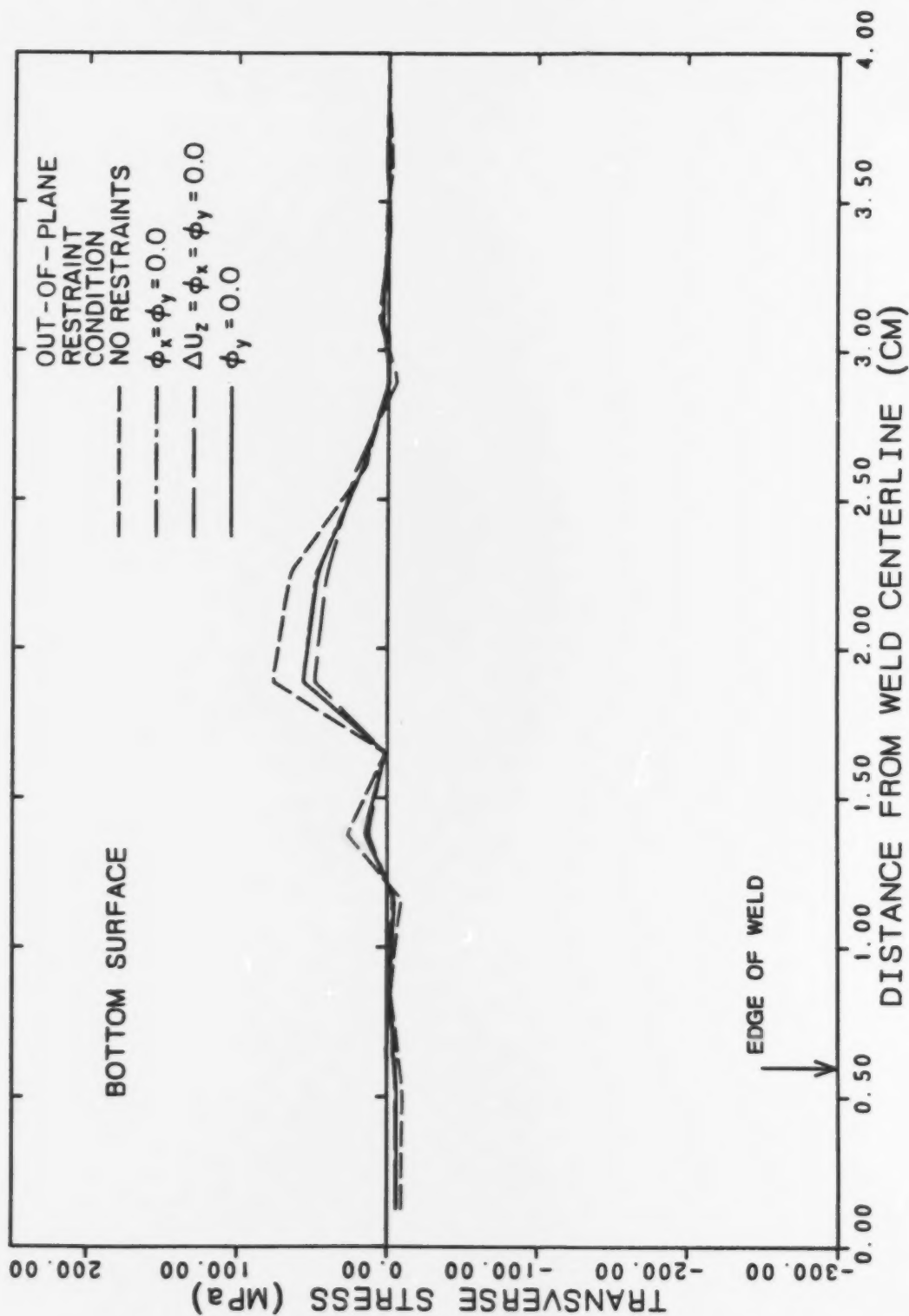


FIGURE 3.36 TRANSVERSE RESIDUAL STRESS DISTRIBUTION ON THE BOTTOM SURFACE
— SIMPLY SUPPORTED PLATE

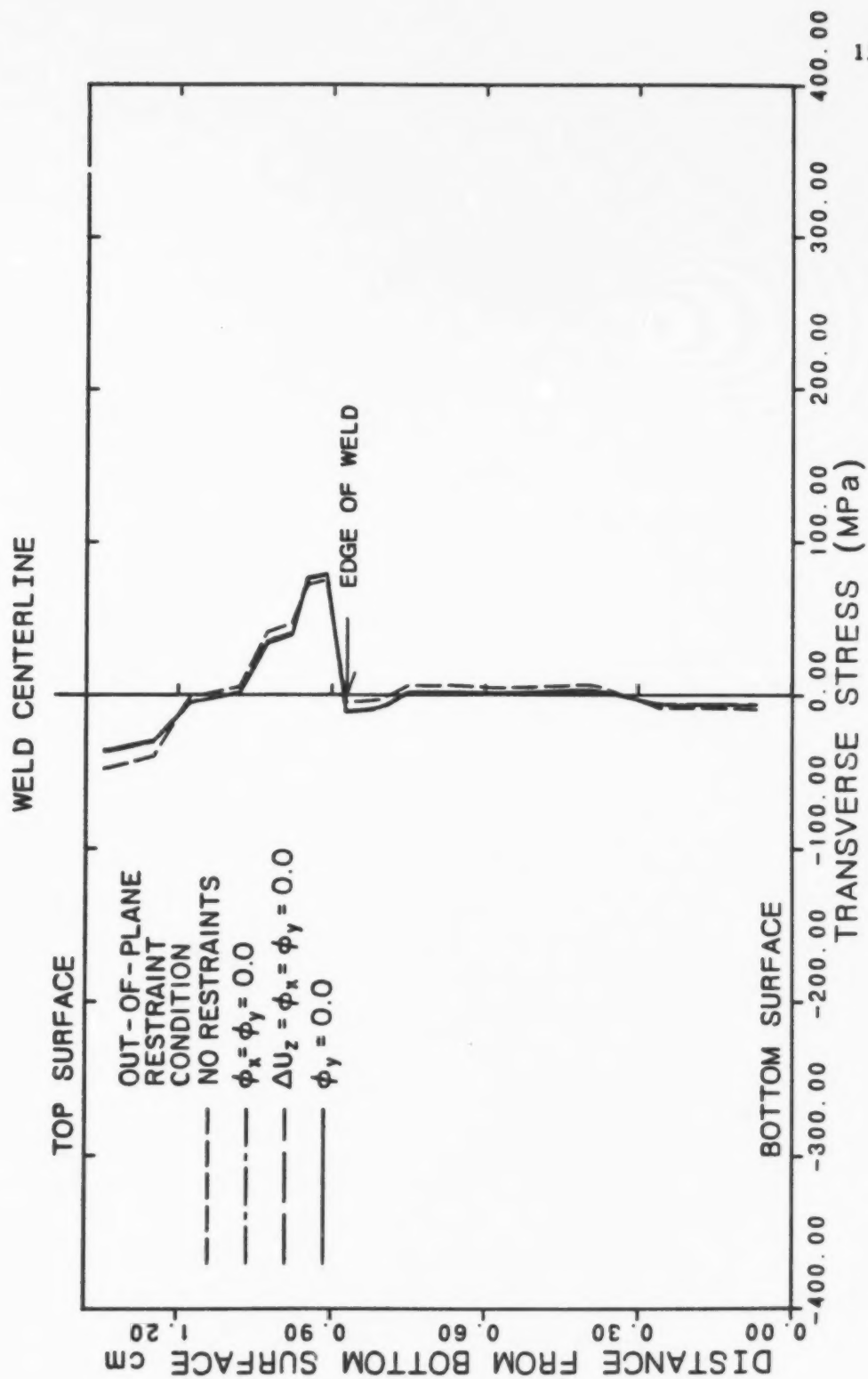


FIGURE 3.37 TRANSVERSE RESIDUAL STRESS DISTRIBUTION ALONG THE WELD CENTERLINE - SIMPLY SUPPORTED PLATE

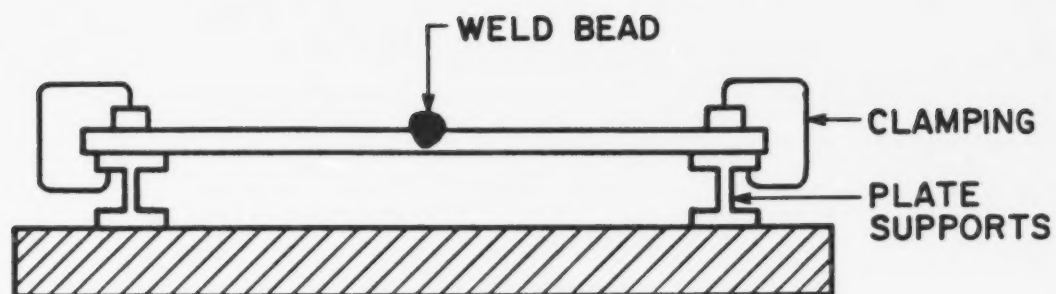


FIGURE 3.38 WELDING SETUP-CLAMPED ALONG BOTH EDGES

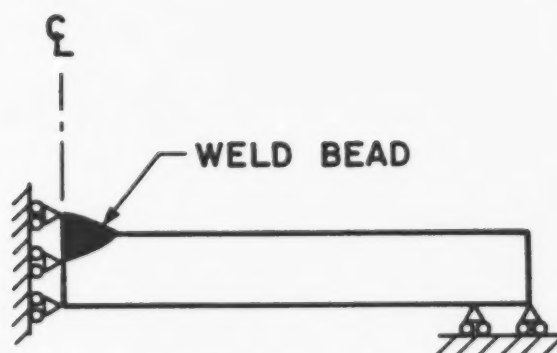


FIGURE 3.39 IN-PLANE KINEMATIC CONSTRAINTS - CLAMPED ALONG BOTH EDGES

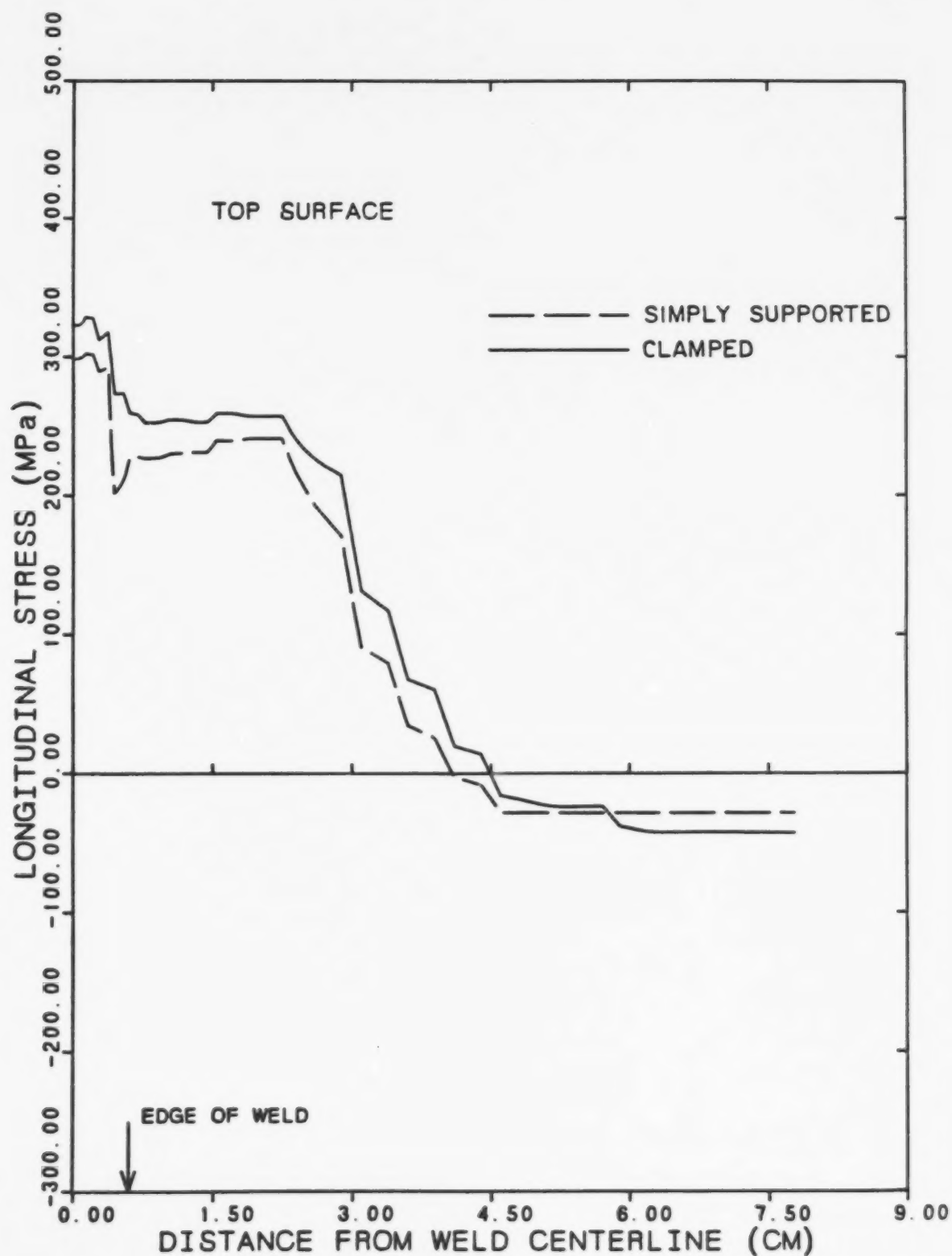


FIGURE 3.40 LONGITUDINAL RESIDUAL STRESS DISTRIBUTION ON THE TOP SURFACE - SIMPLY SUPPORTED VS. CLAMPED

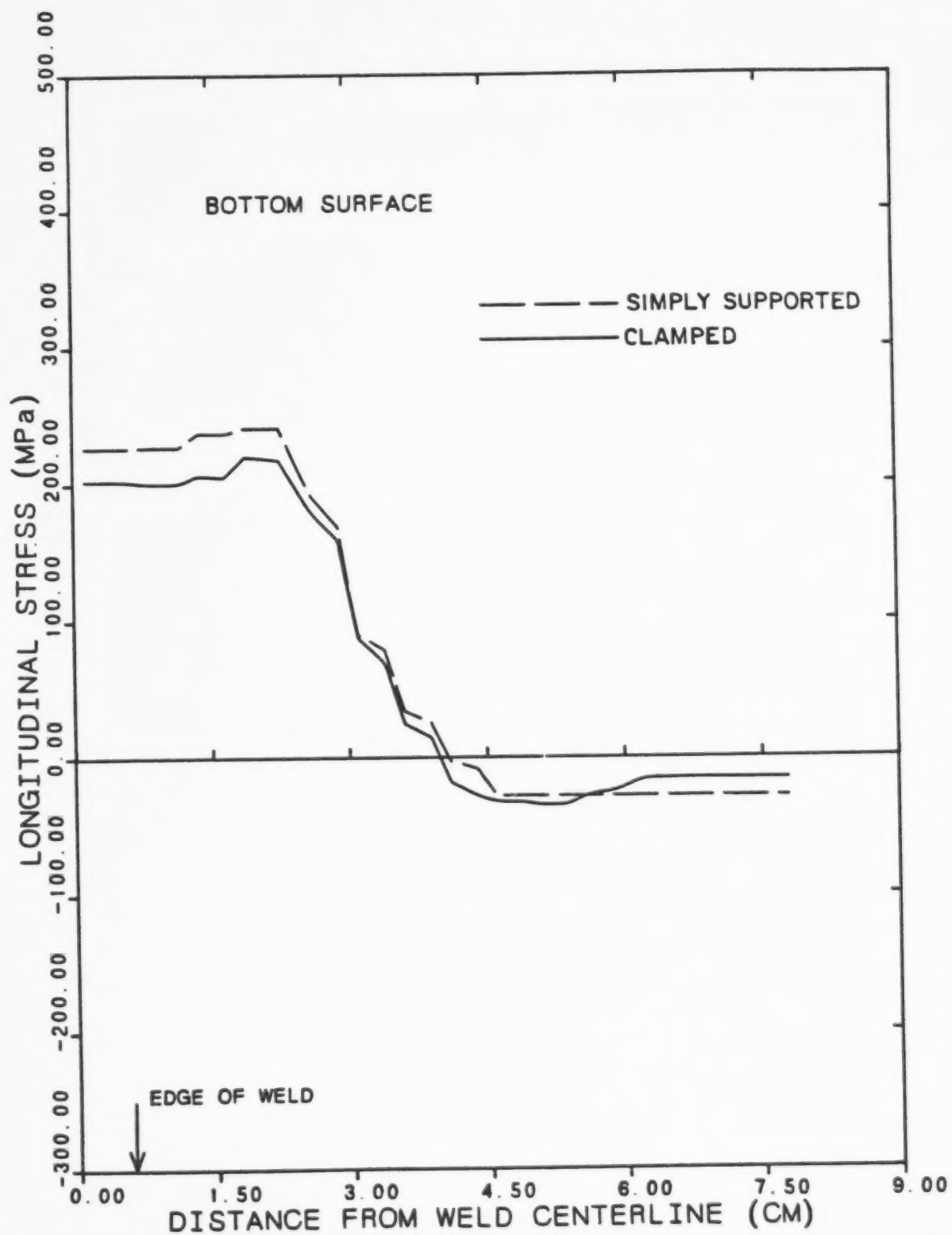


FIGURE 3.41 LONGITUDINAL RESIDUAL STRESS DISTRIBUTION ON THE BOTTOM SURFACE—SIMPLY SUPPORTED VS CLAMPED

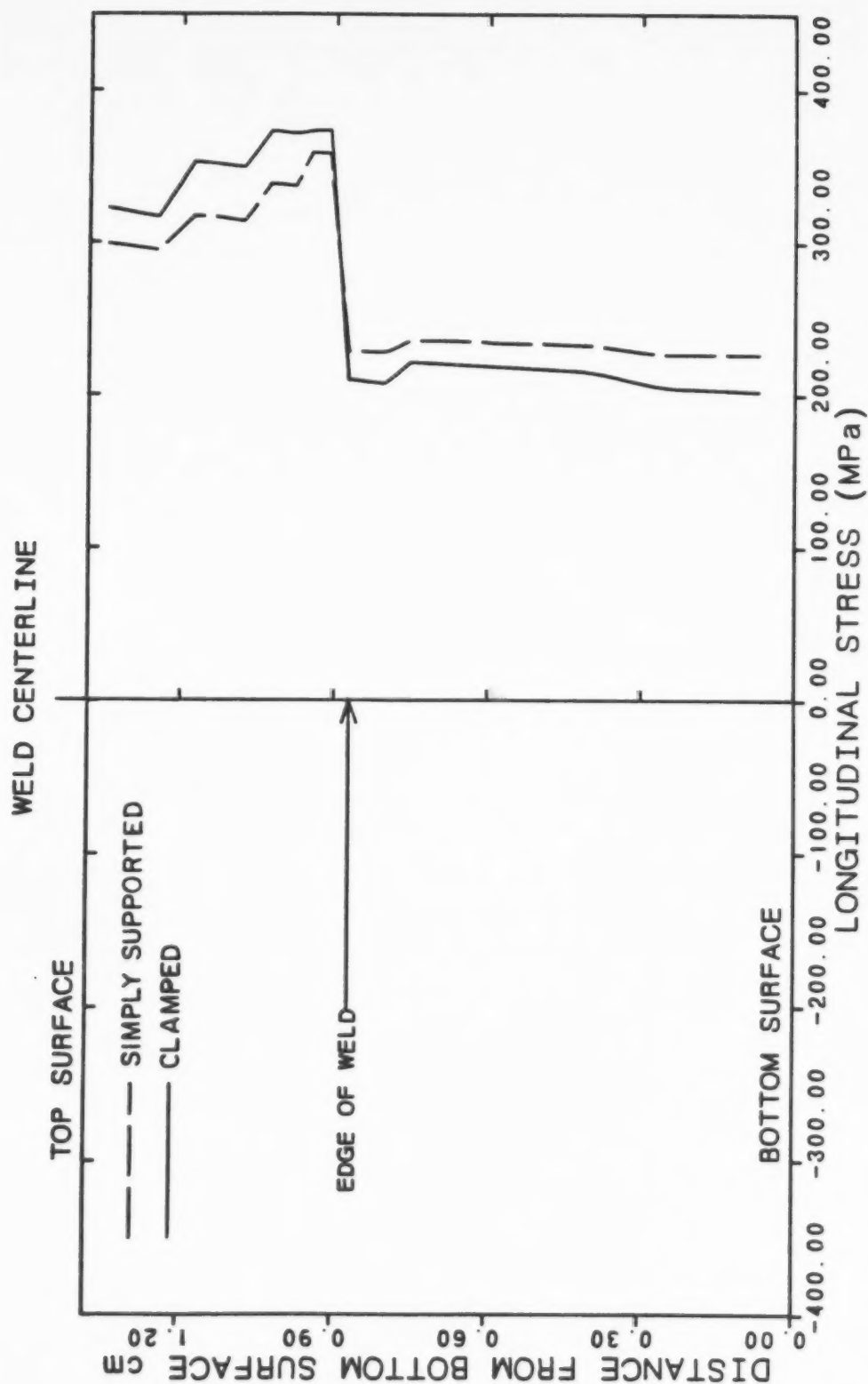


FIGURE 3.42 LONGITUDINAL RESIDUAL STRESS DISTRIBUTION ALONG THE WELD CENTERLINE
SIMPLY SUPPORTED VS CLAMPED

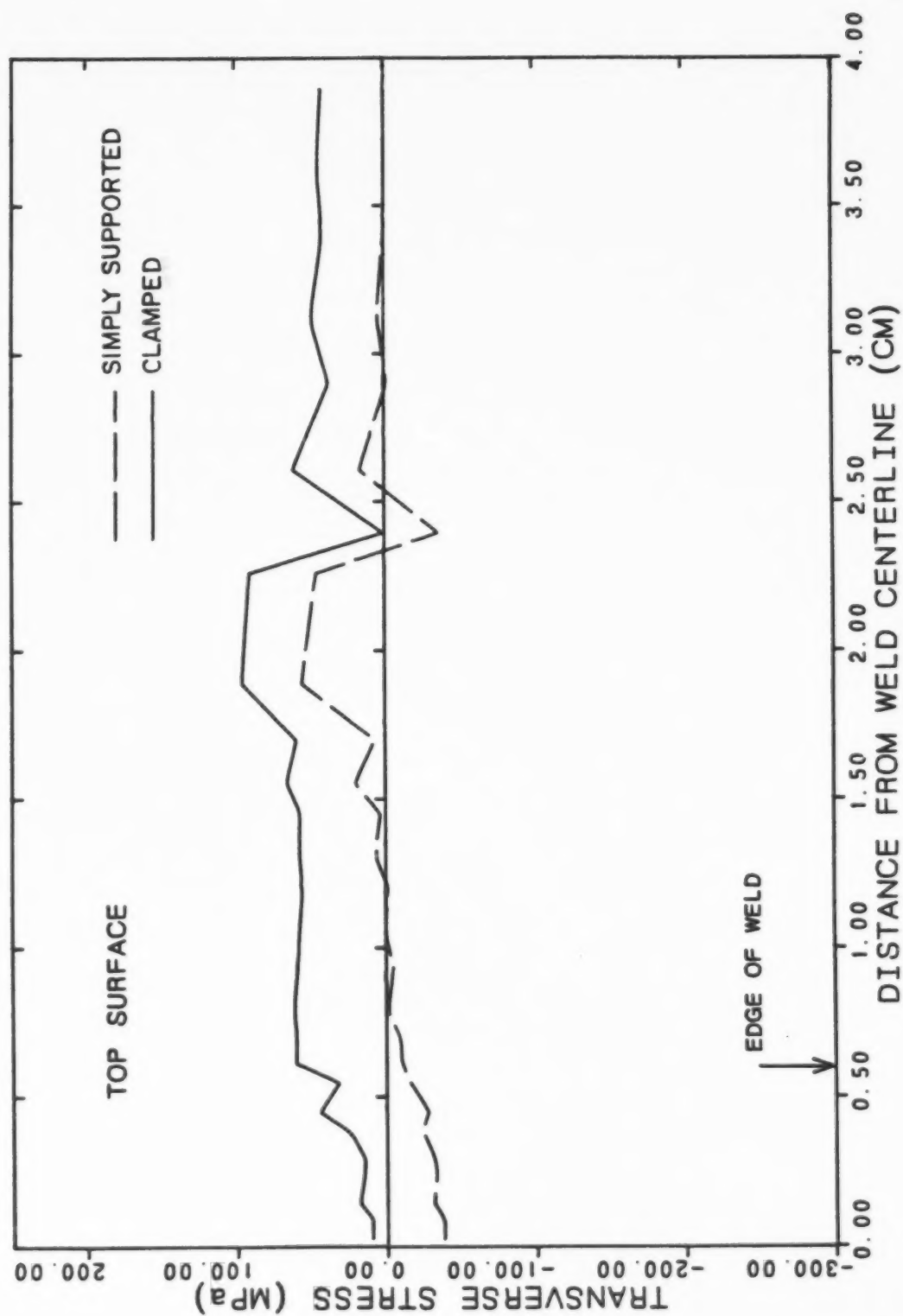


FIGURE 3.43 TRANSVERSE RESIDUAL STRESS DISTRIBUTION ON THE TOP SURFACE -
SIMPLY SUPPORTED VS. CLAMPED

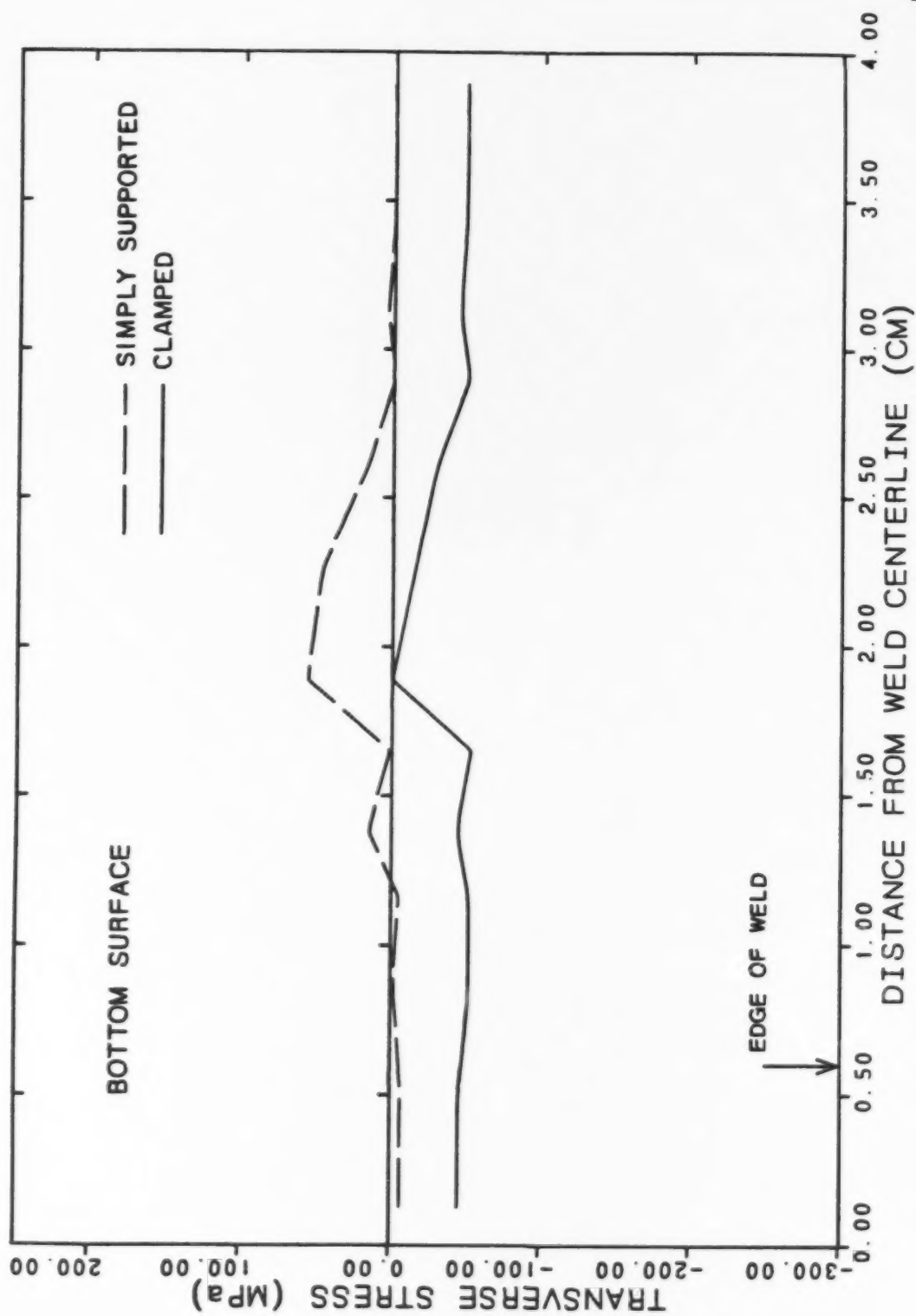


FIGURE 3.44 TRANSVERSE RESIDUAL STRESS DISTRIBUTION ON THE BOTTOM SURFACE
— SIMPLY SUPPORTED VS CLAMPED

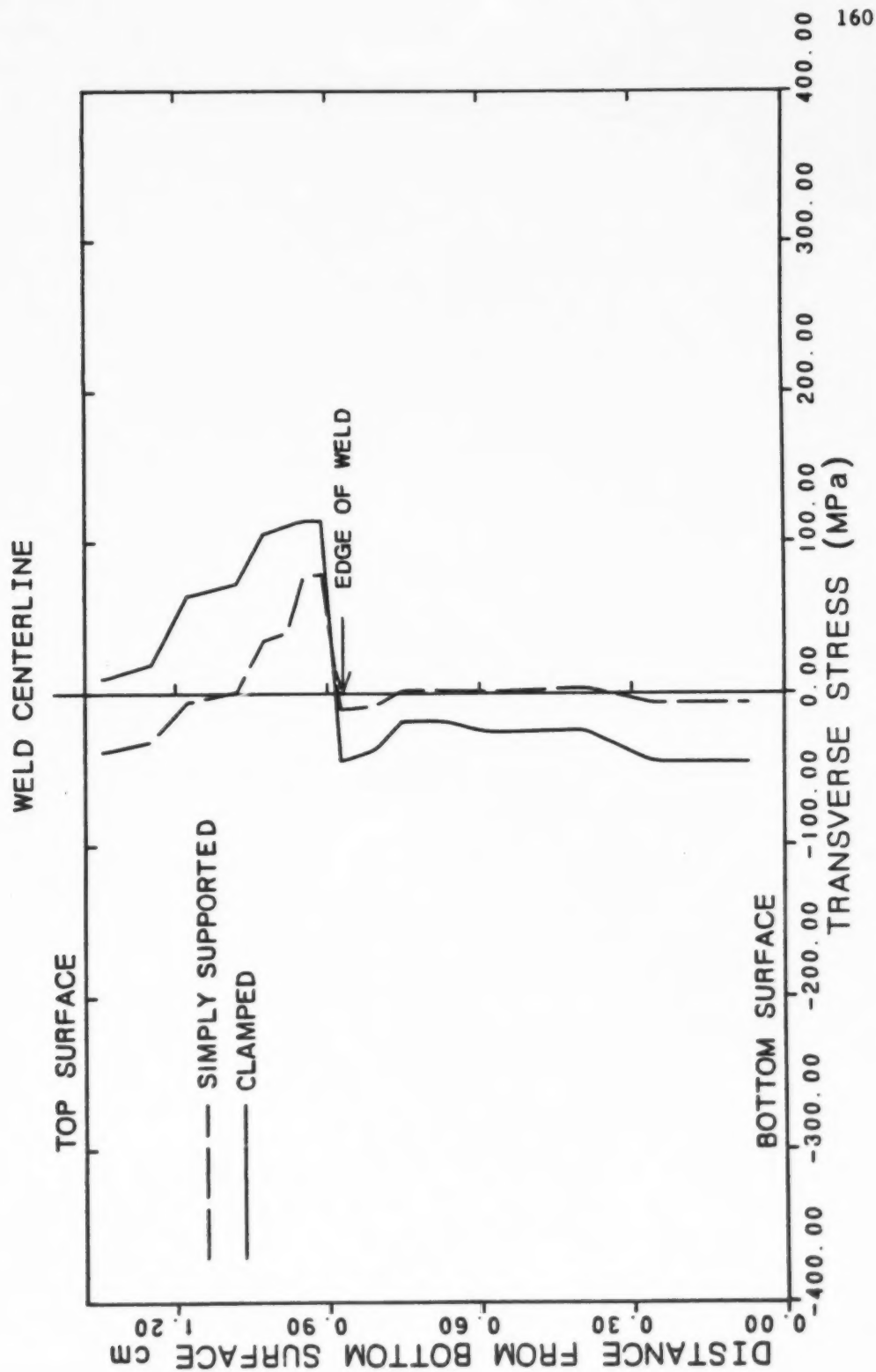


FIGURE 3.45 TRANSVERSE RESIDUAL STRESS DISTRIBUTION ALONG THE WELD CENTRELNE
— SIMPLY SUPPORTED VS CLAMPED

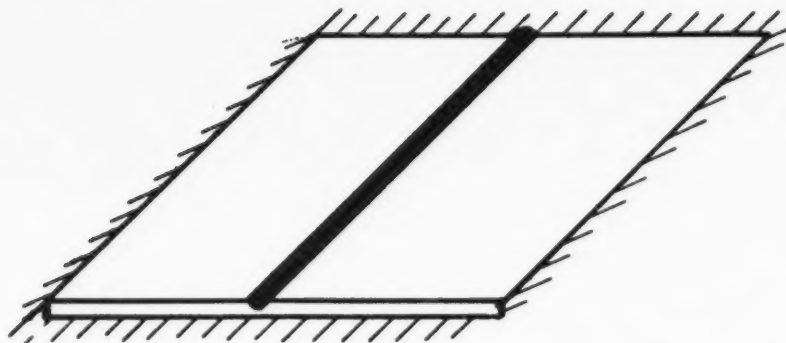


FIGURE 3.46 FULLY RESTRAINED PLATE



FIGURE 3.47 IN-PLANE KINEMATIC CONSTRAINTS—FULLY RESTRAINED PLATE

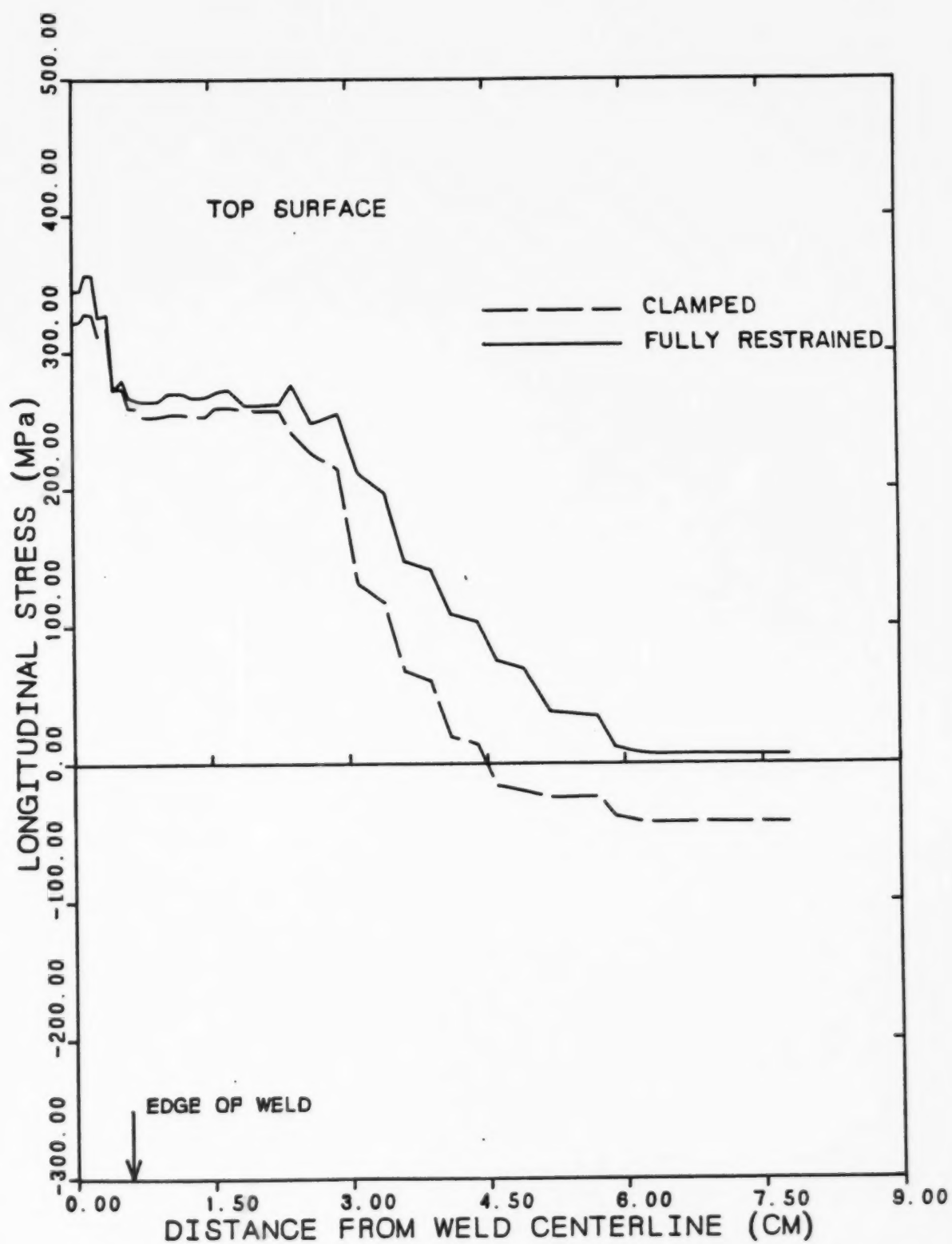


FIGURE 3.48 LONGITUDINAL RESIDUAL STRESS DISTRIBUTION ON TOP SURFACE - CLAMPED VS FULLY RESTRAINED

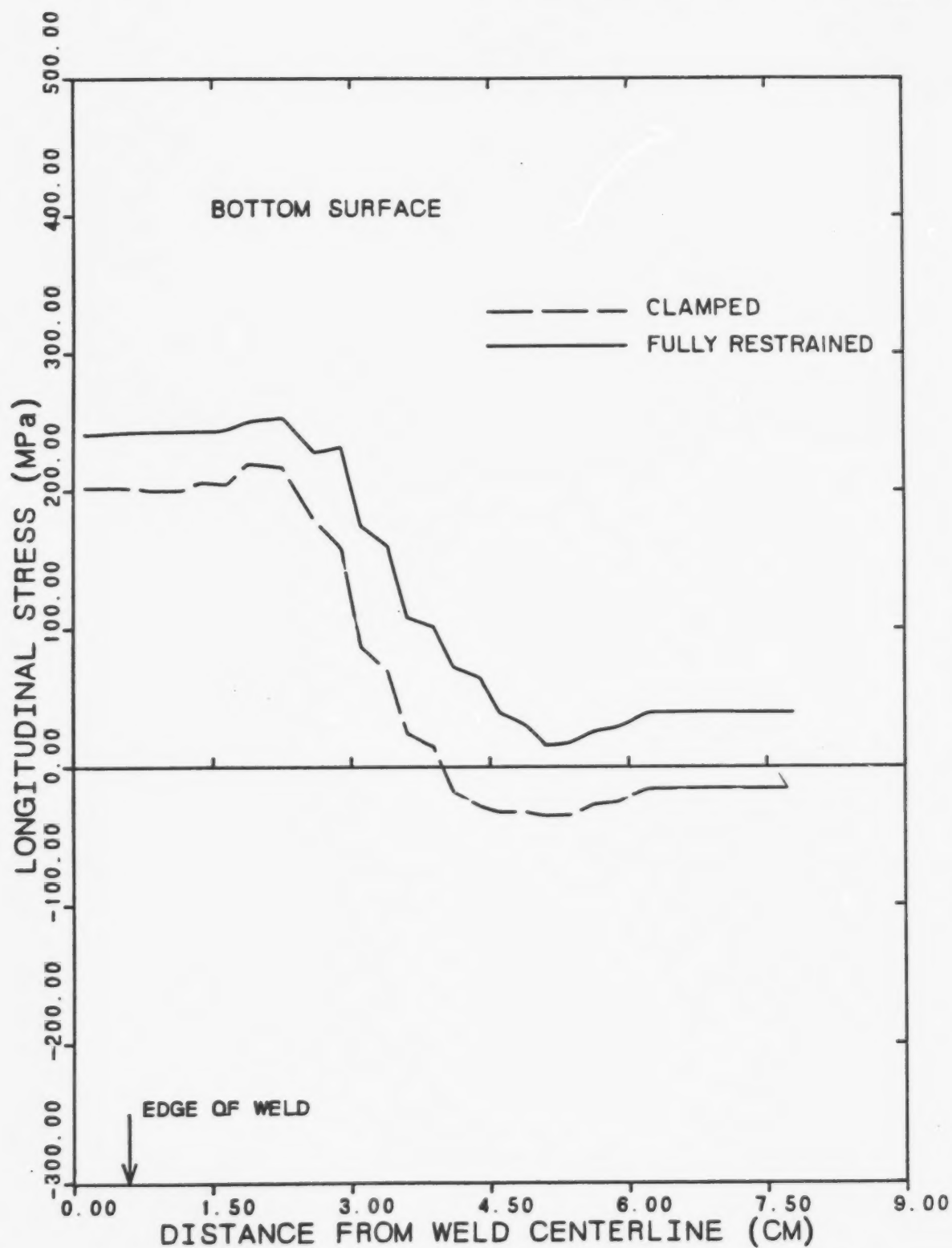


FIGURE 3.49 LONGITUDINAL RESIDUAL STRESS DISTRIBUTION ON THE BOTTOM SURFACE - CLAMPED VS FULLY RESTRAINED

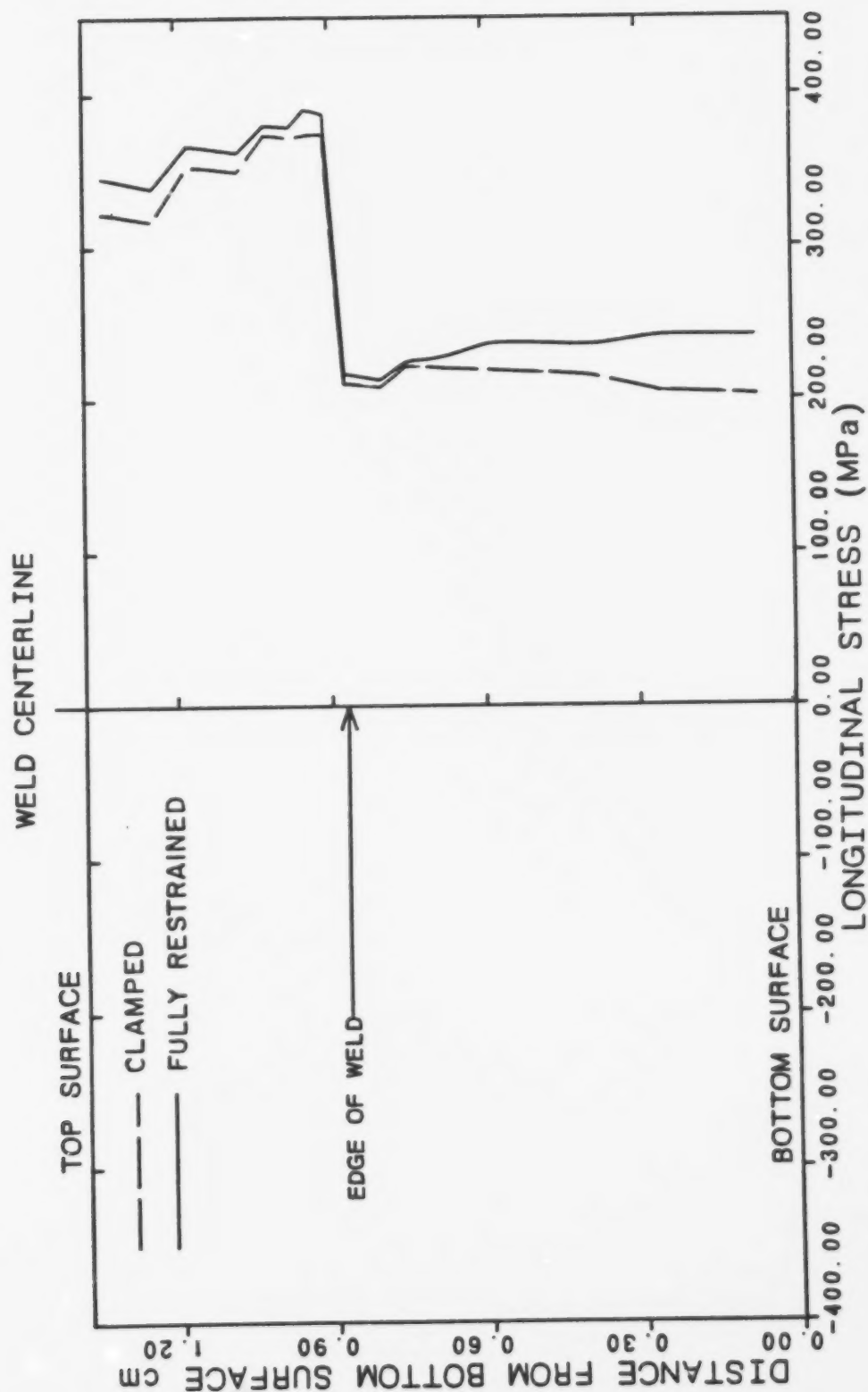


FIGURE 3.50 LONGITUDINAL RESIDUAL STRESS DISTRIBUTION ALONG THE WELD CENTERLINE
- CLAMPED VS FULLY RESTRAINED

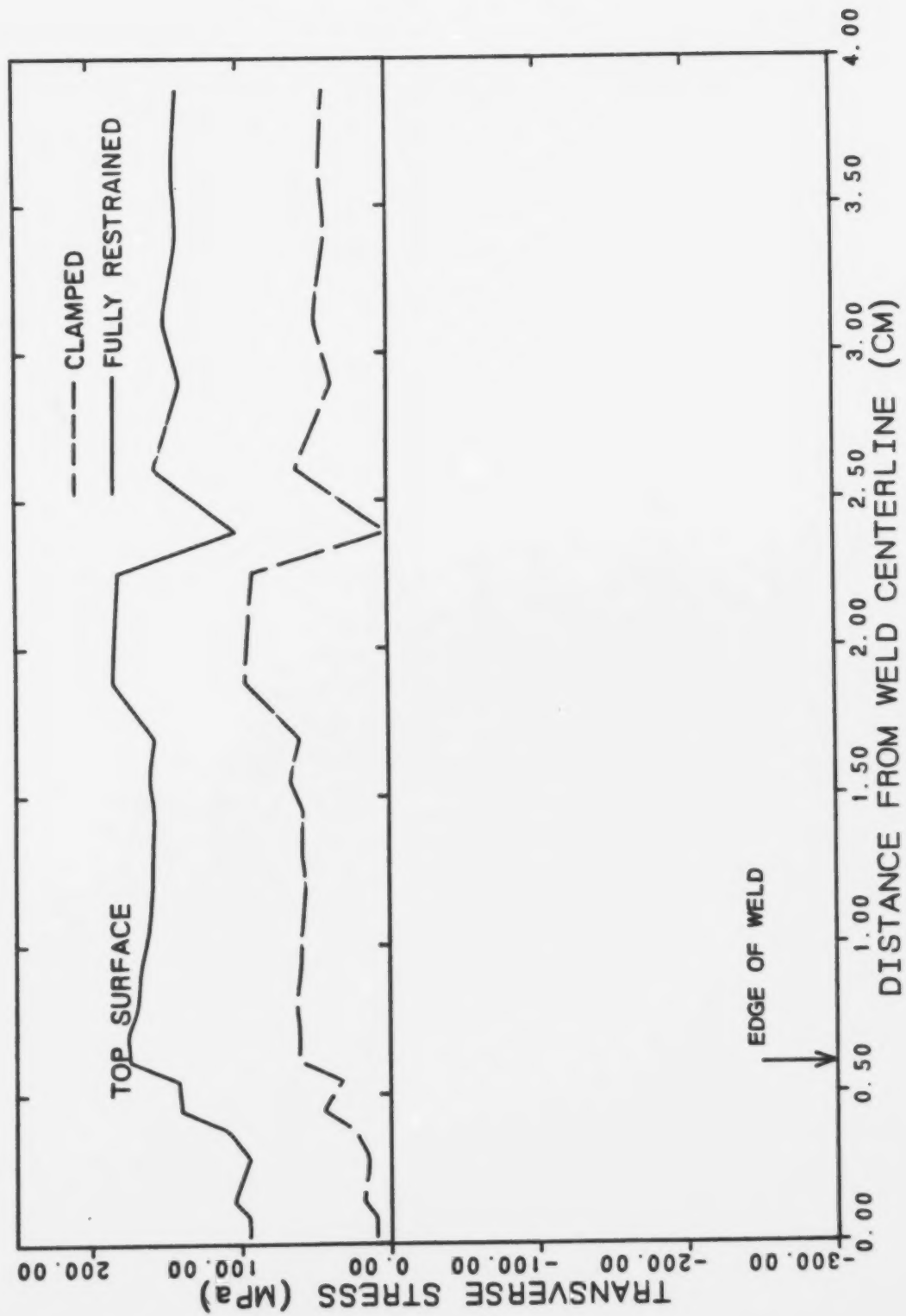


FIGURE 3.51 TRANSVERSE RESIDUAL STRESS DISTRIBUTION ON THE TOP SURFACE
- CLAMPED VS FULLY RESTRAINED

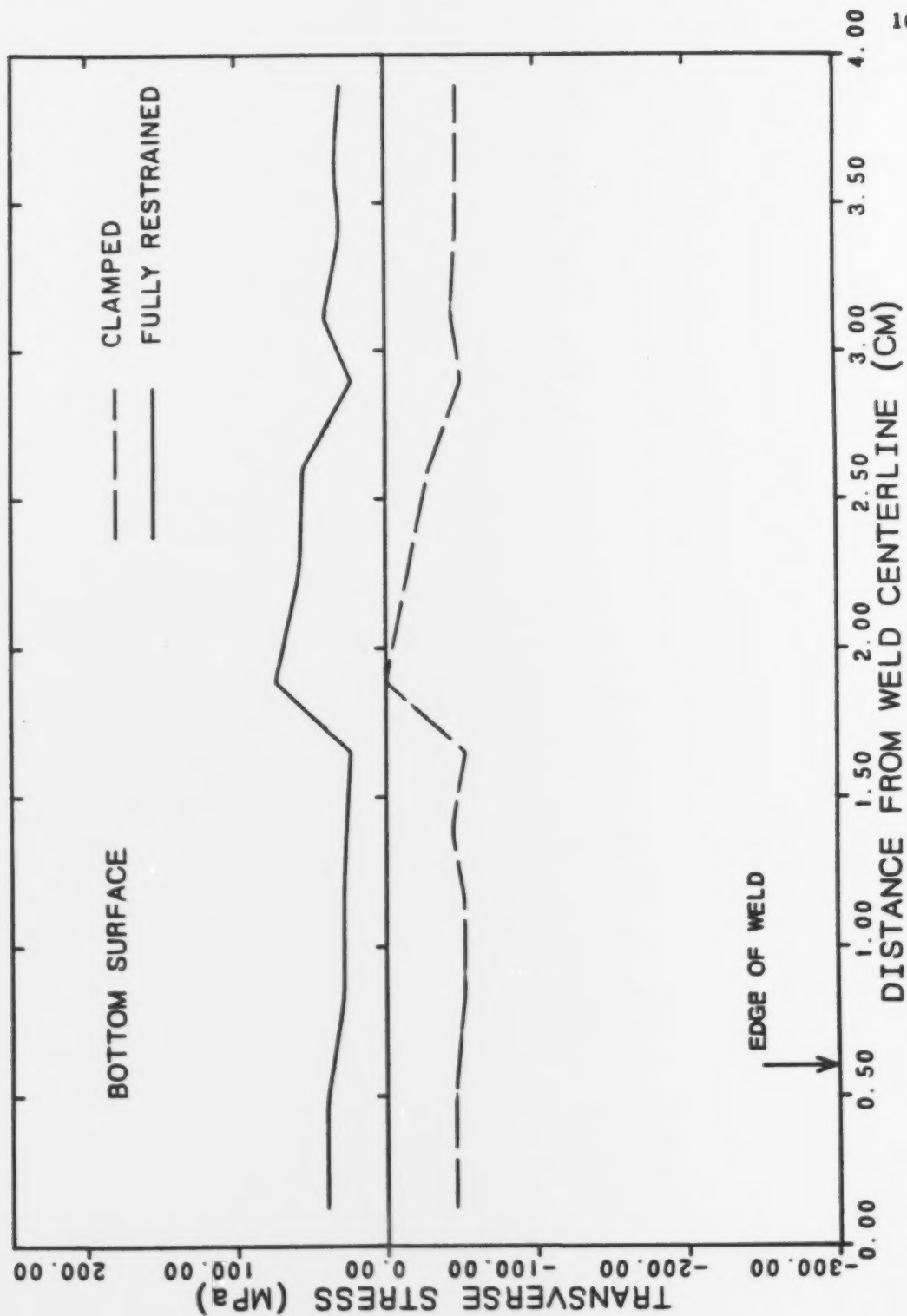


FIGURE 3.52 TRANSVERSE RESIDUAL STRESS DISTRIBUTION ON THE BOTTOM SURFACE
—CLAMPED VS FULLY RESTRAINED

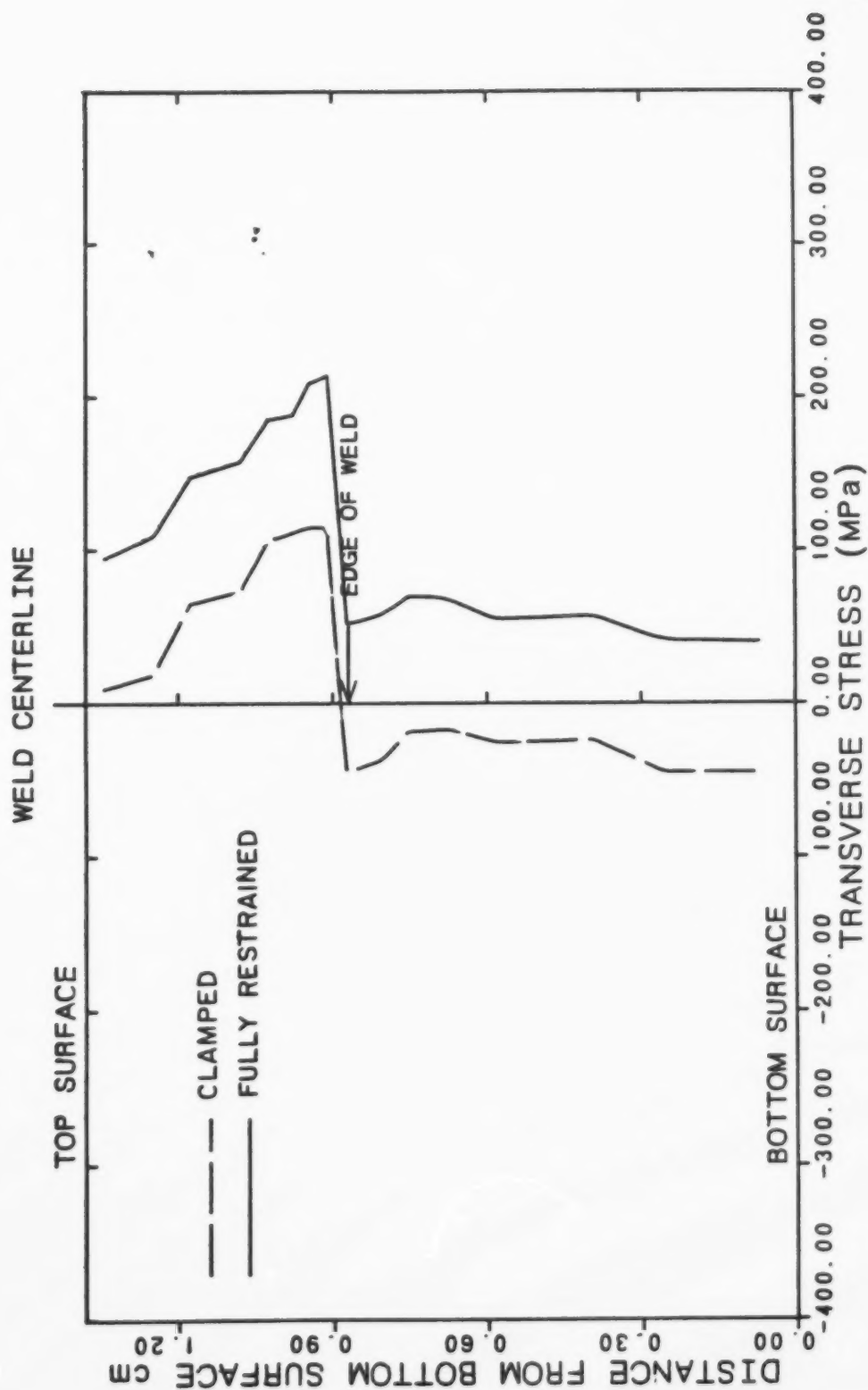


FIGURE 3.53 TRANSVERSE RESIDUAL STRESS DISTRIBUTION ALONG THE WELD CENTERLINE
—CLAMPED VS FULLY RESTRAINED

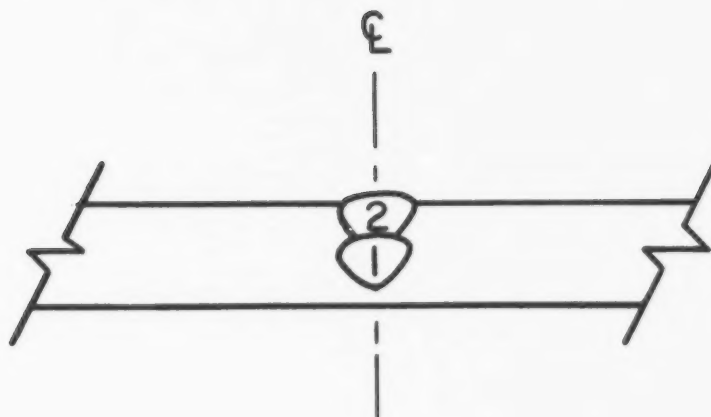


FIGURE 4.1 TWO PASSES LAID VERTICALLY ON EACH OTHER

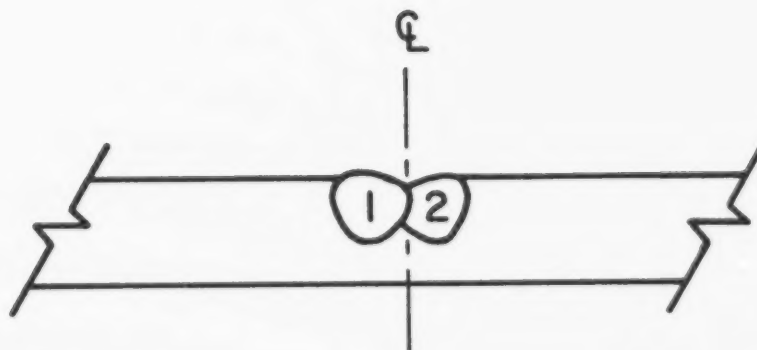


FIGURE 4.2 TWO PASSES LAID SIDE BY SIDE

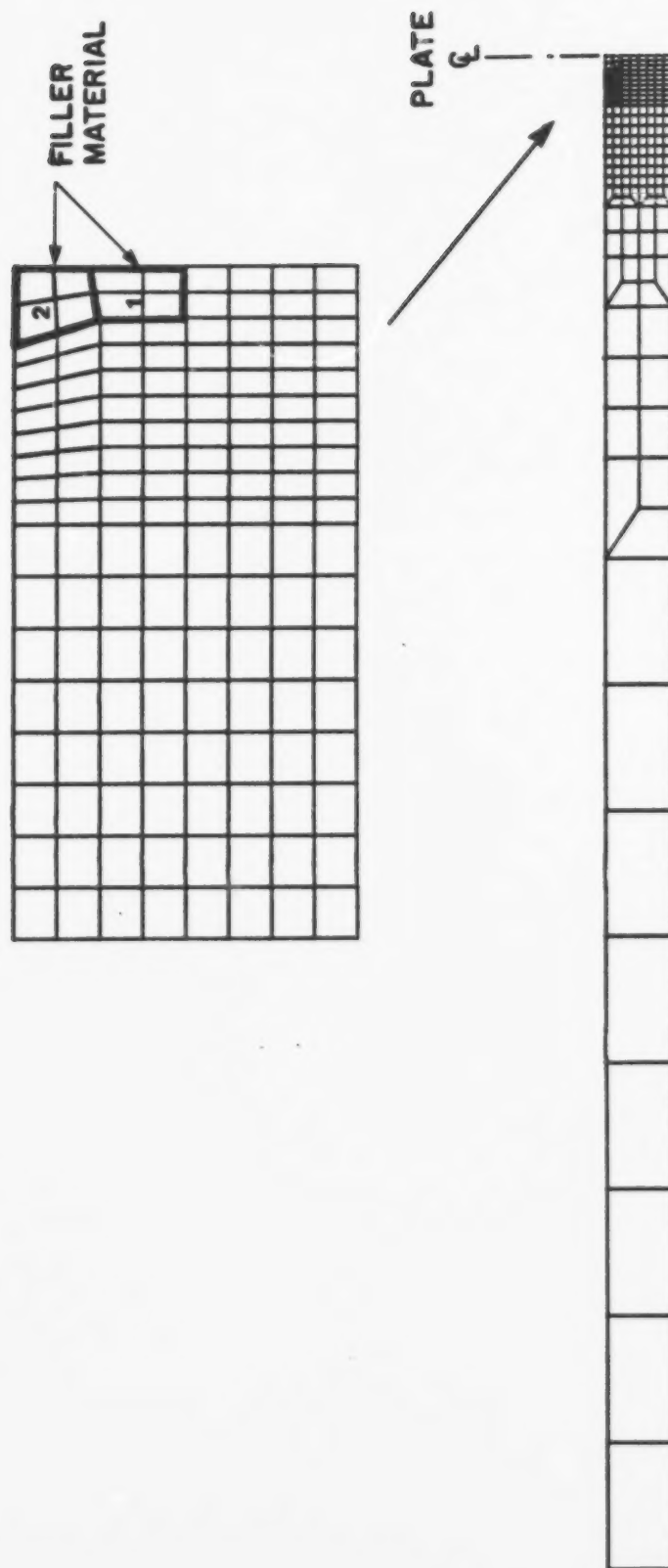


FIGURE 4.3 FINITE ELEMENT MESH - TWO PASSES LAID VERTICALLY

ABAQUS VERSION 4-S-147

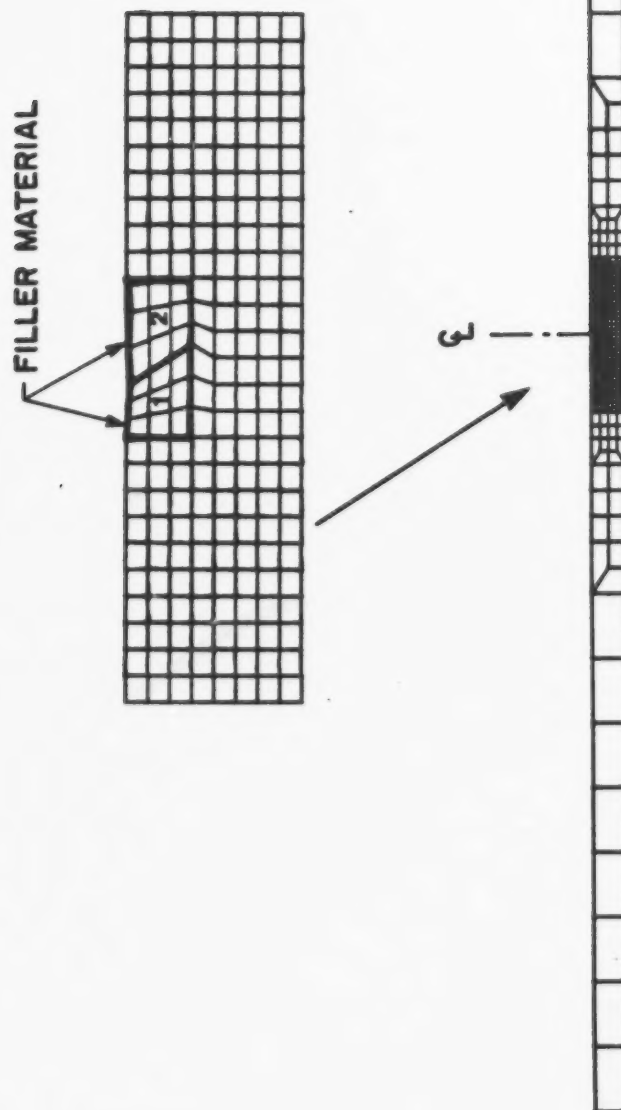


FIGURE 4.4 FINITE ELEMENT MESH - TWO PASSES LAID SIDE BY SIDE

ABAQUS VERSION 4.5-1417

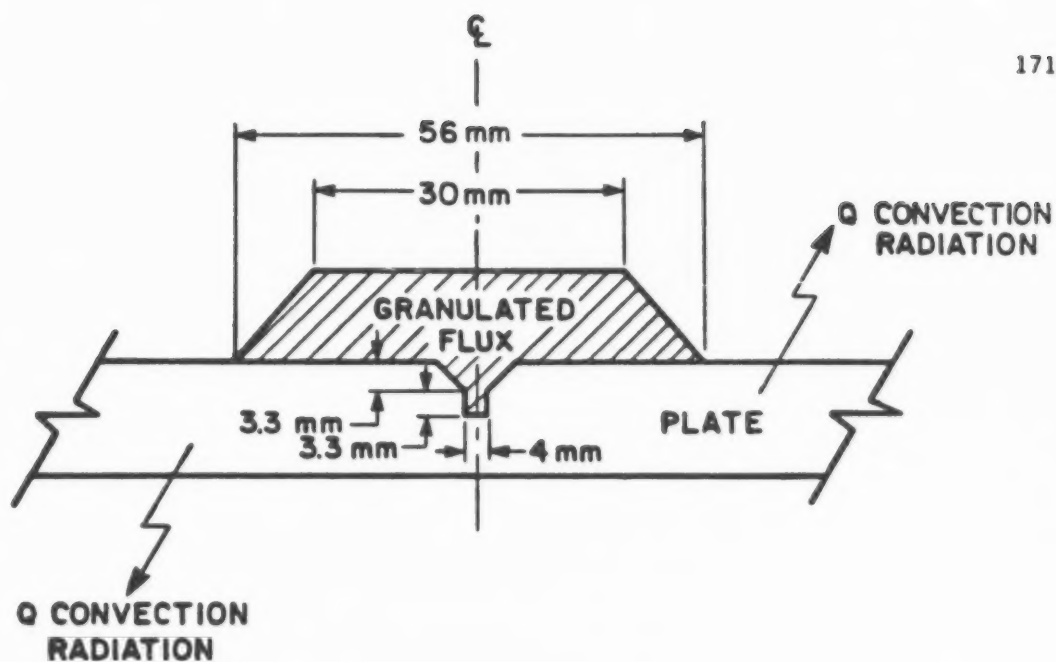


FIGURE 4.5 THERMAL BOUNDARY CONDITIONS - TWO PASSES LAID VERTICALLY

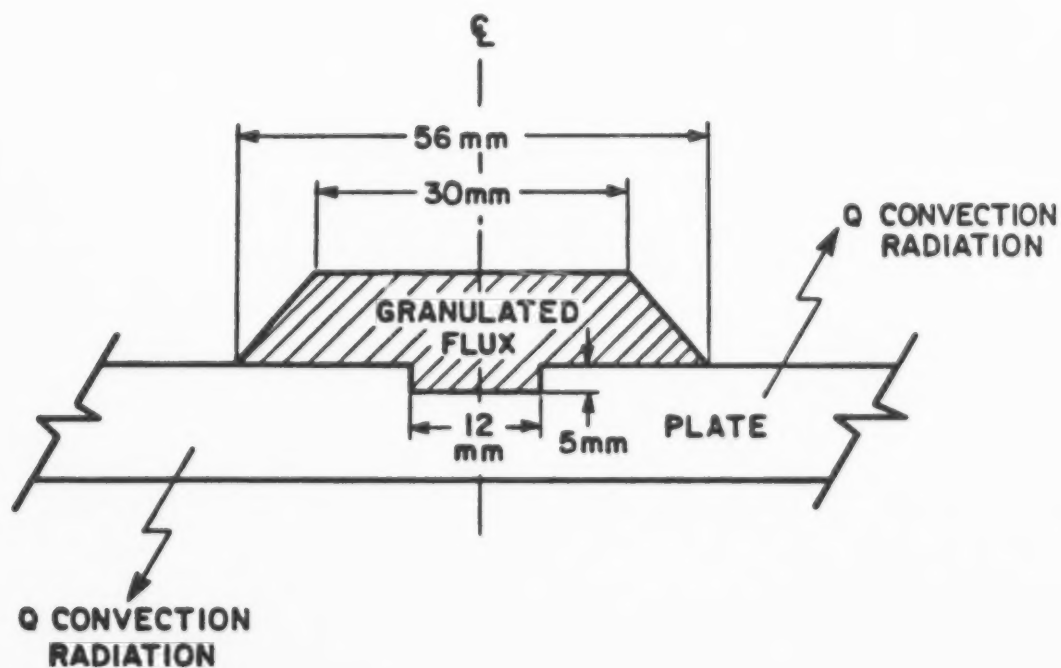


FIGURE 4.6 THERMAL BOUNDARY CONDITIONS - TWO PASSES LAID SIDE BY SIDE

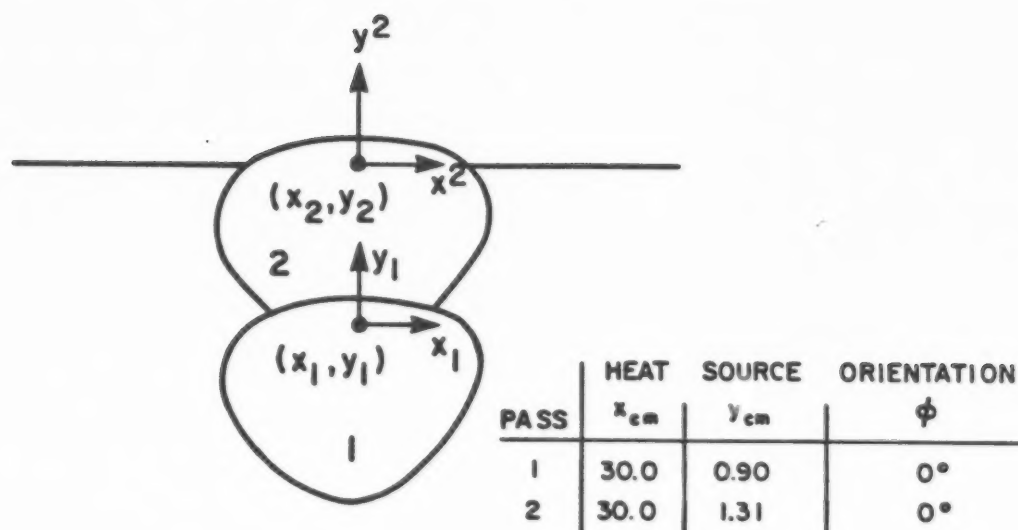


FIGURE 4.7 HEAT SOURCE LOCATION - TWO PASSES LAID VERTICALLY

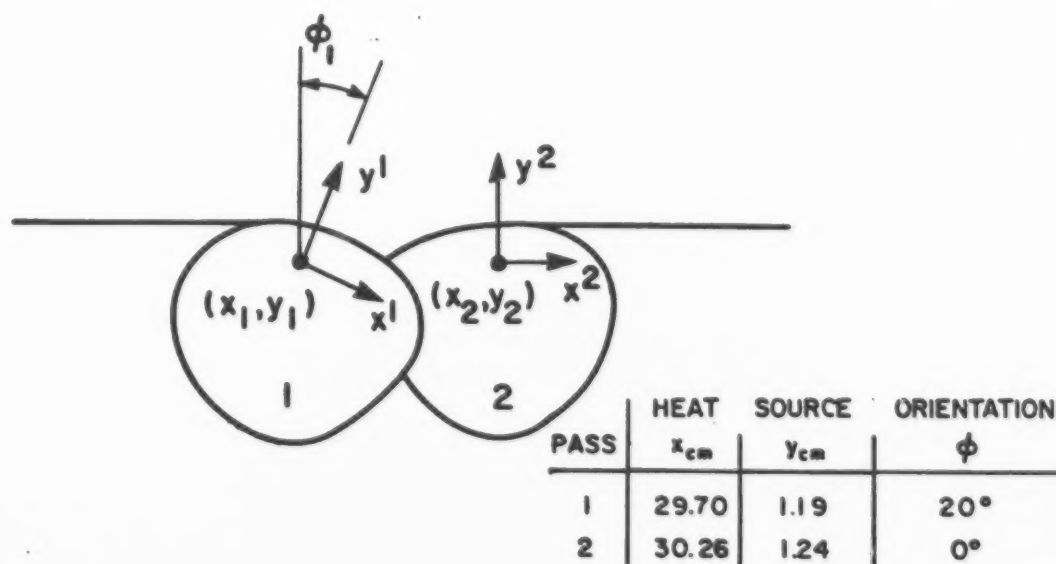


FIGURE 4.8 HEAT SOURCE LOCATION - TWO PASSES LAID SIDE BY SIDE

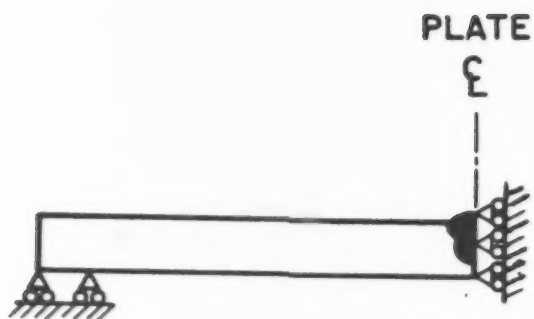


FIGURE 4.9 IN-PLANE KINEMATIC CONSTRAINTS -
TWO PASSES LAID VERTICALLY



FIGURE 4.10 IN-PLANE KINEMATIC CONSTRAINTS -
TWO PASSES LAID SIDE BY SIDE

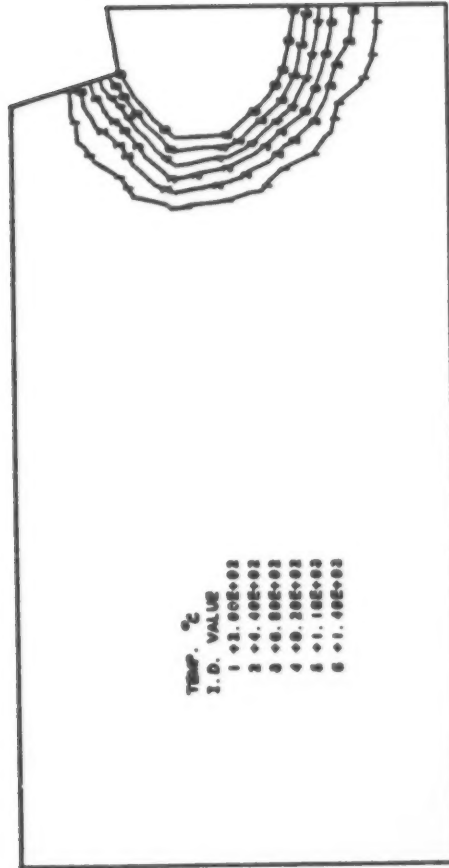


FIGURE 4.11 WELD POOL SHAPE (TWO PASSES LAID VERTICALLY) - PASS 1
STEP 3 INCREMENT 30
ABAQUS VERSION 4-2-147
174

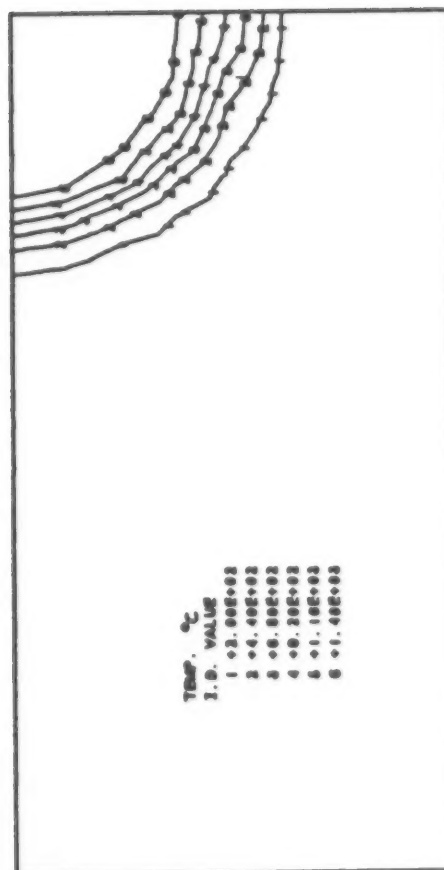


FIGURE 4.12 WELD POOL SHAPE (TWO PASSES LAID VERTICALLY) - PASS 2

STEP 8 INCREMENT 40

ABRUS VERSION 4-5-147

DISPL.
MAX. FACTOR = 0.0000
SOLID LINES - DISTORTED MESH
DASHED LINES - ORIGINAL MESH

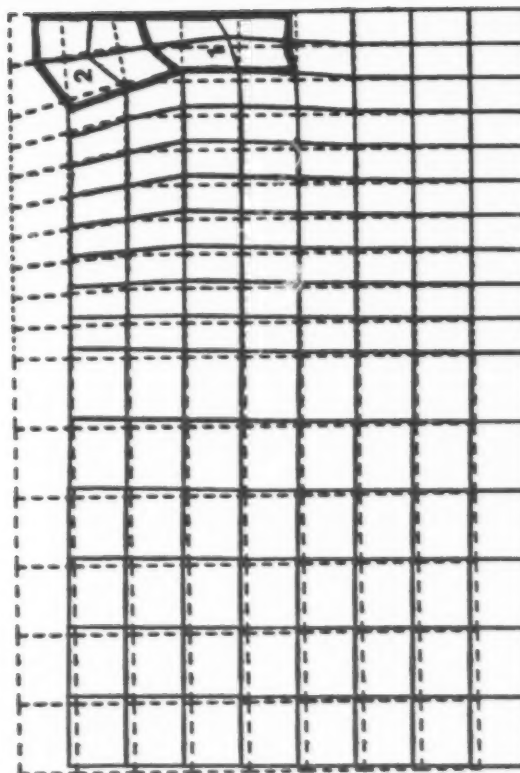


FIGURE 4.13 DISTORTED MESH PLOT (TWO PASSES LAID VERTICALLY)
STEP 6 INCREMENT 2
ABAQUS VERSION 4.5-1.0

DISPL. FACTOR = 0.0E+00
 MAG. FACTOR = 0.0E+00
 SOLID LINES - DISPLACED MESH
 DASHED LINES - ORIGINAL MESH



FIGURE 4.14 DEFORMED PLATE GEOMETRY AFTER WELDING (TWO PASSES LAID VERTICALLY)
 STEP 8 INCREMENT 2
 ABAQUS VERSION 4.6-147

Stress
I.D.

1
2
3
4
5
6
7
8
9
10
11

MPa

200
220
240
260
280
300
320
340
360
380
400

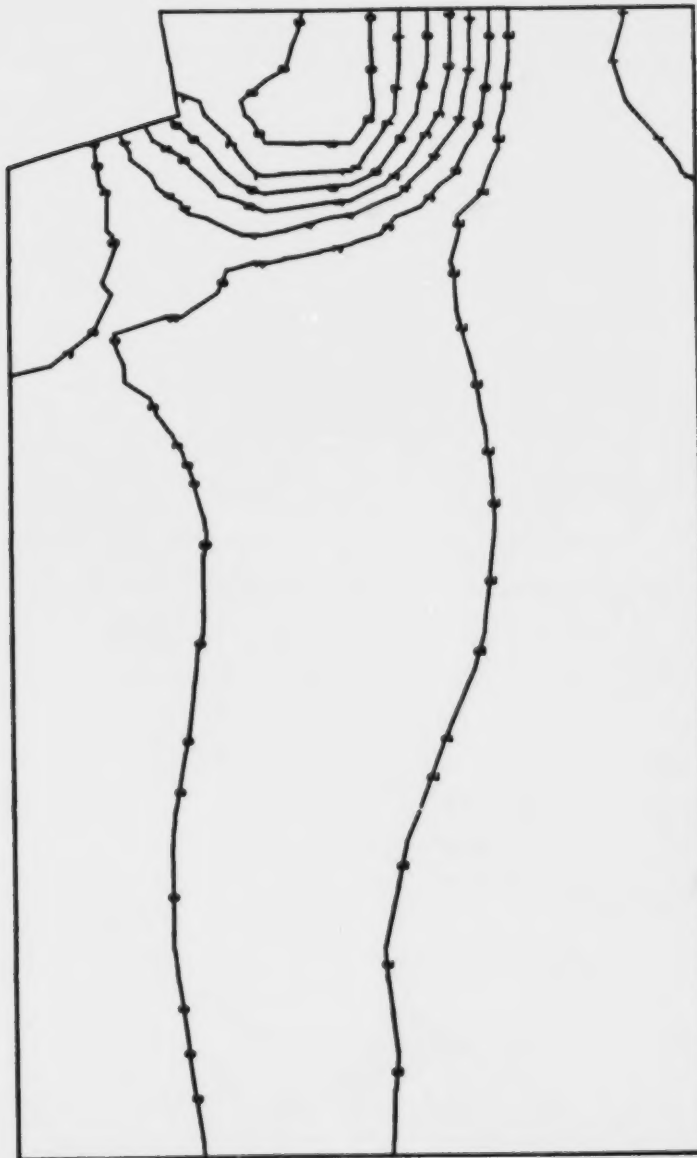


FIGURE 4.15 LONG. RESIDUAL STRESSES (TWO PASSES LAID VERTICALLY) - PASS 1

ASACUS VERSION 4-5-147

STEP 3 INCREMENT 2

Stress I.D.	MPa
1	200
2	220
3	240
4	260
5	280
6	300
7	320
8	340
9	360
10	380
11	400

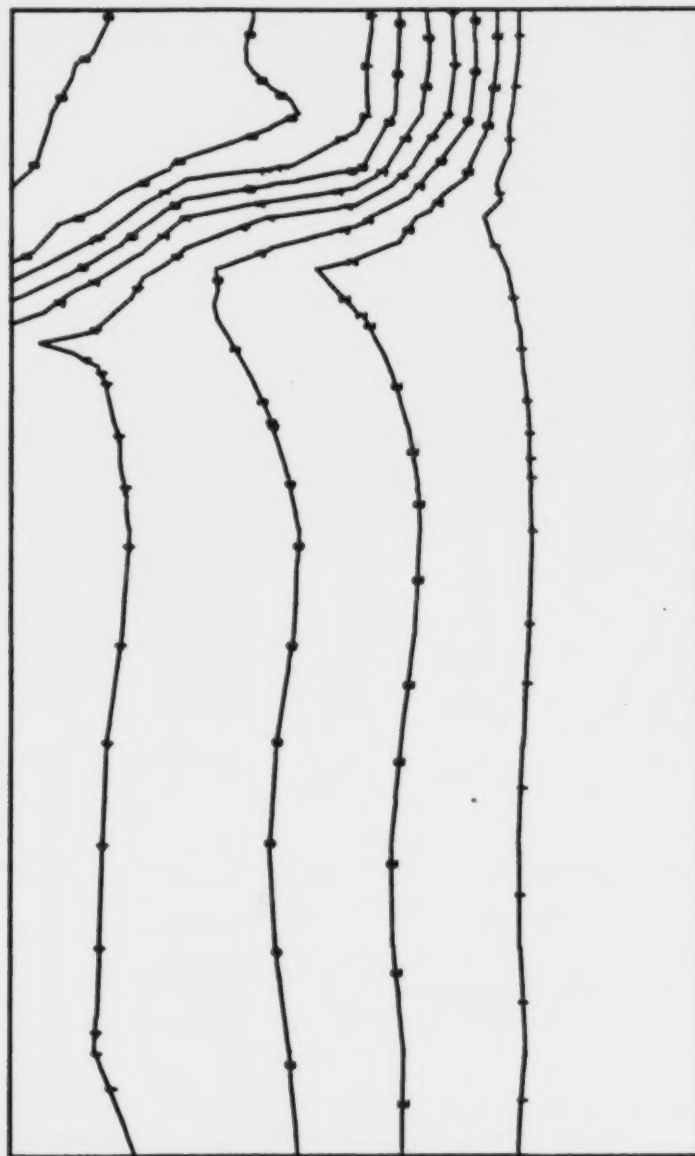


FIGURE 4.16 LONG. RESIDUAL STRESSES (TWO PASSES LAID VERTICALLY) - PASS 2 179

STEP 5 INCREMENT 2

ABAQUS VERSION 4.3-147

Stress
I.D.

1
2
3
4
5
6
7
8
9
10
11

MPa

-100
-80
-60
-40
-20
0
+20
+40
+60
+80
+100

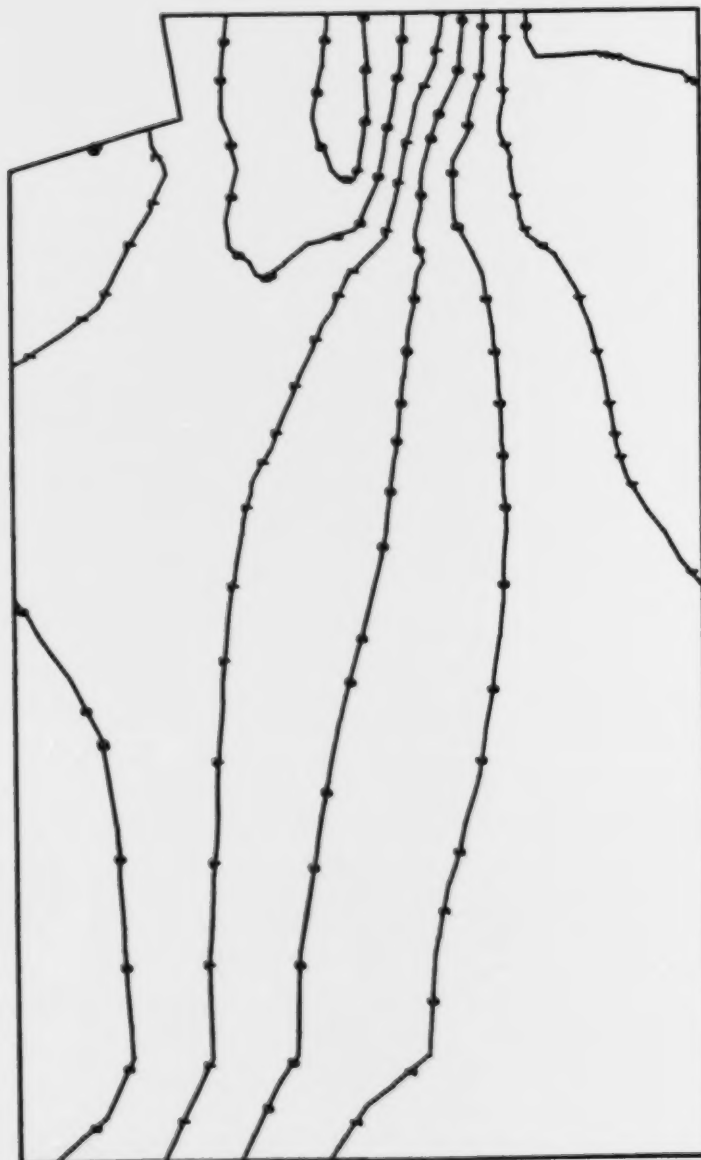


FIGURE 4.17 TRANSVERSE RESIDUAL STRESSES (TWO PASSES LAID VERTICALLY) - PASS 1

STEP 3 INCREMENT 2

ABAQUS VERSION 4.5-117

Stress
I.D.

MPa

1
2
3
4
5
6
7
8
9
10
11

-100
-80
-60
-40
-20
0
+20
+40
+60
+80
+100

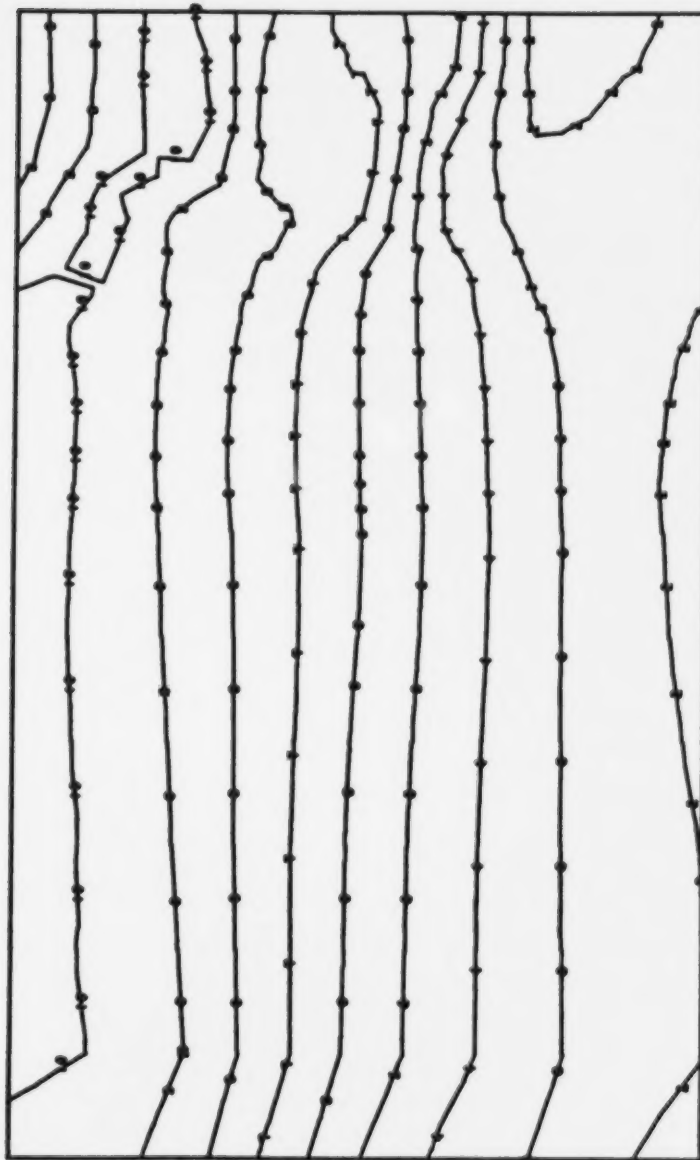


FIGURE 4.18 TRANSVERSE RESIDUAL STRESSES (TWO PASSES LAID VERTICALLY) - PASS 2

ABAQUS VERSION 4.5-147

STEP 5 INCREMENT 2

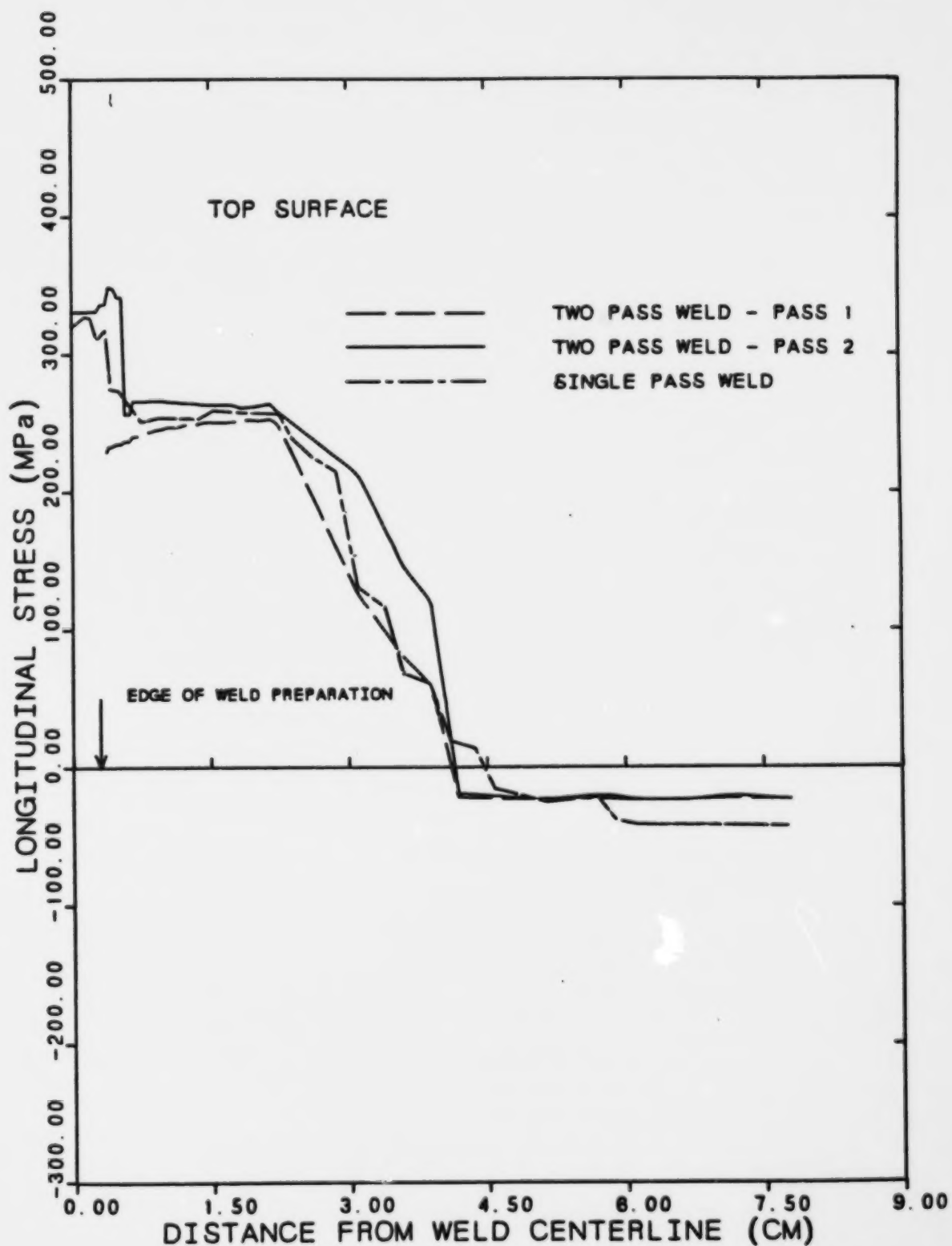


FIGURE 4.19 LONG. RESIDUAL STRESS DISTRIBUTION ON THE TOP SURFACE - TWO PASSES LAID VERTICALLY

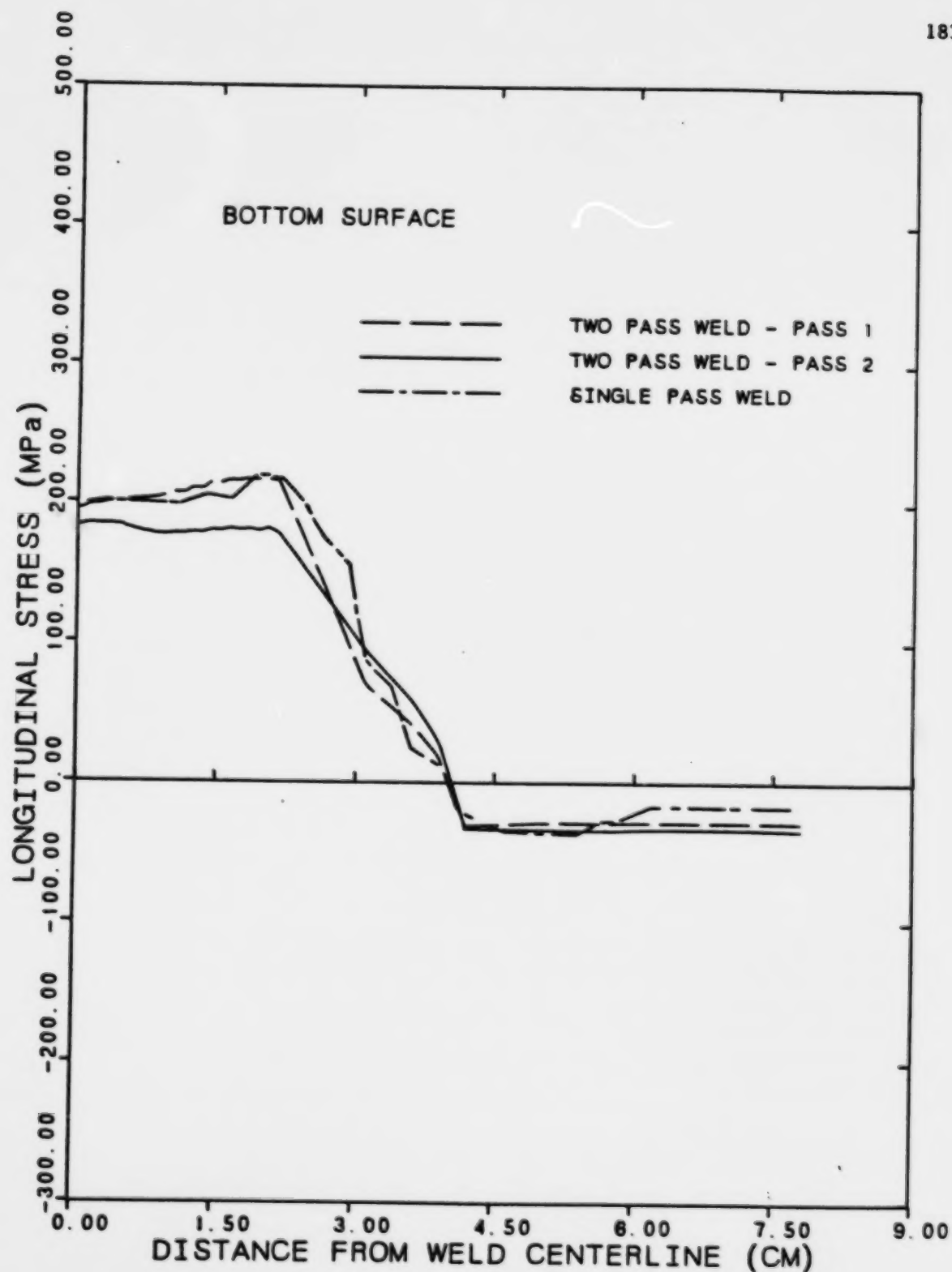


FIGURE 4.20 LONG. RESIDUAL STRESS DISTRIBUTION ON THE
BOTTOM SURFACE - TWO PASSES LAID VERTICALLY

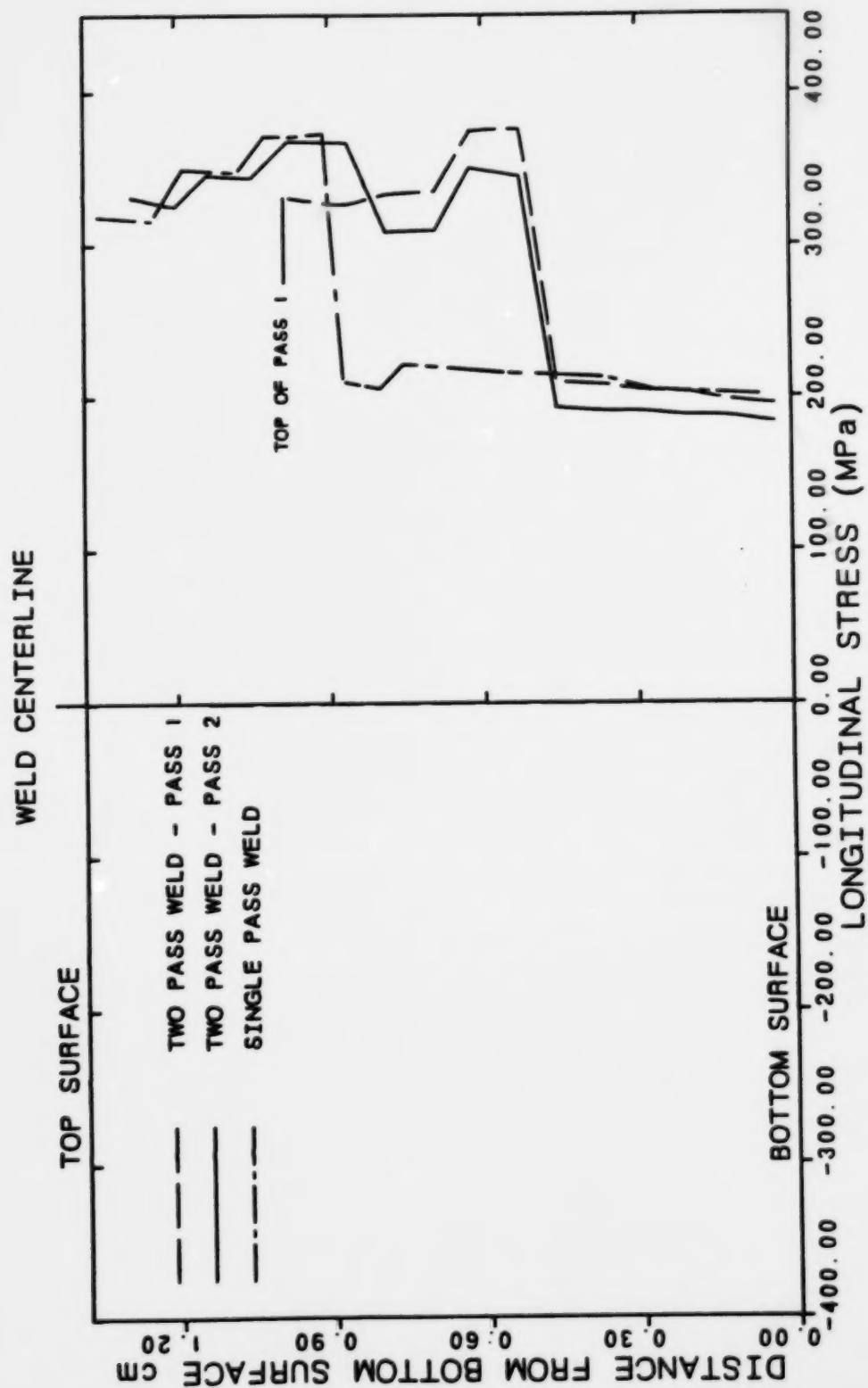


FIGURE 4.21 LONG. RESIDUAL STRESS DISTRIBUTION ALONG THE WELD CENTERLINE
TWO PASSES LAID VERTICALLY

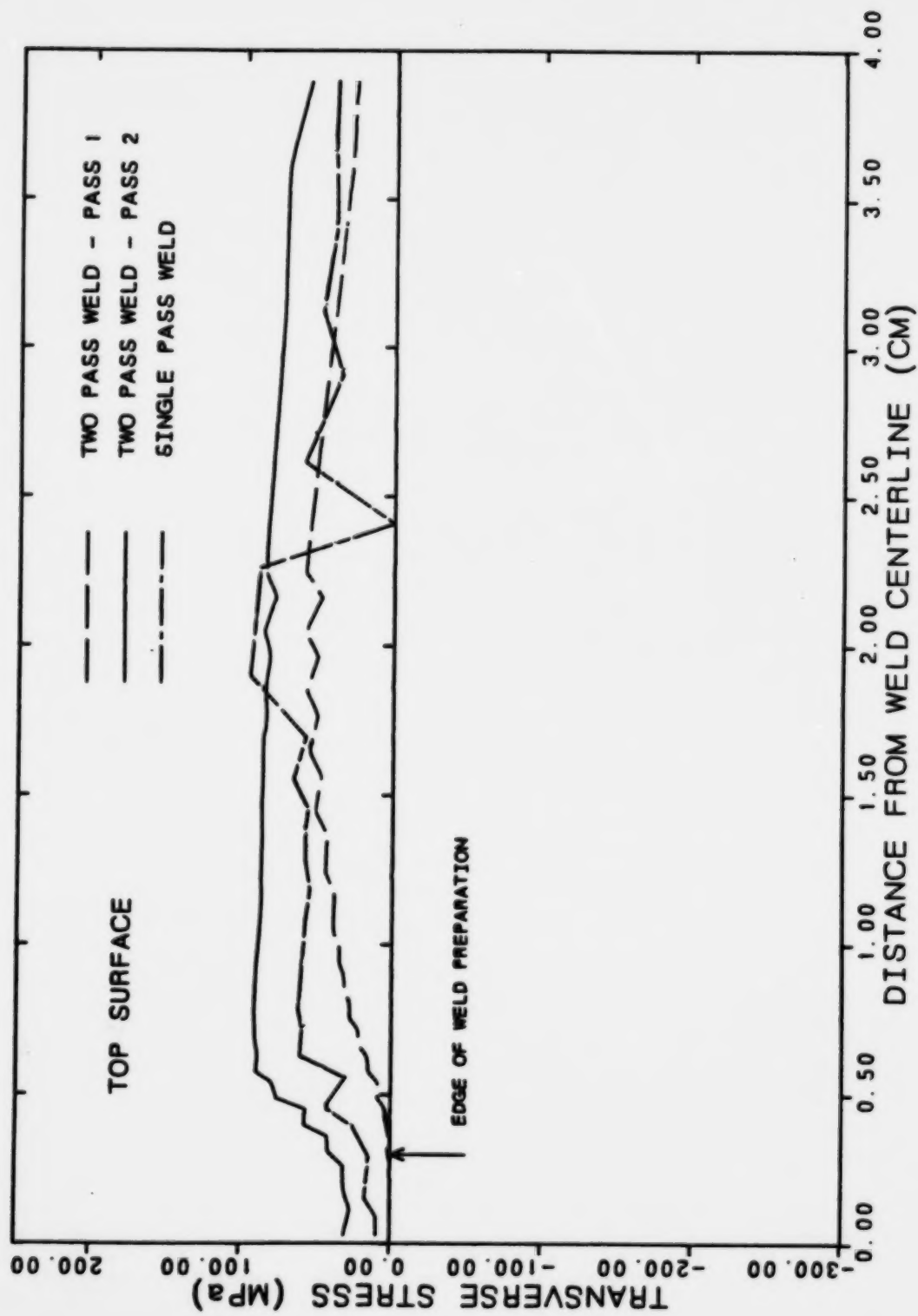


FIGURE 4.22 TRANSVERSE RESIDUAL STRESS DISTRIBUTION ON THE TOP SURFACE
TWO PASSES LAID VERTICALLY

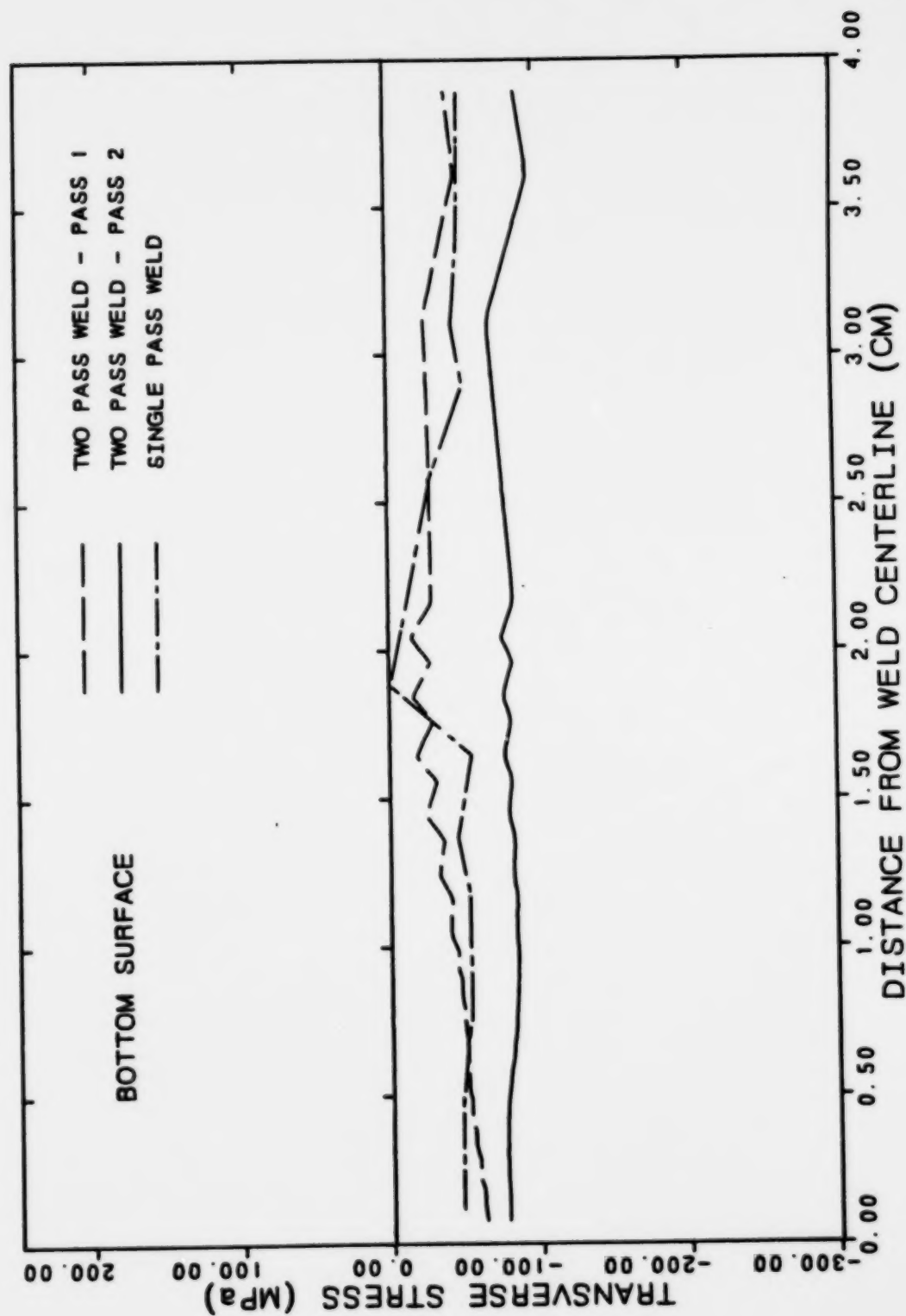


FIGURE 4.23 TRANSVERSE RESIDUAL STRESS DISTRIBUTION ON THE BOTTOM SURFACE
TWO PASSES LAID VERTICALLY

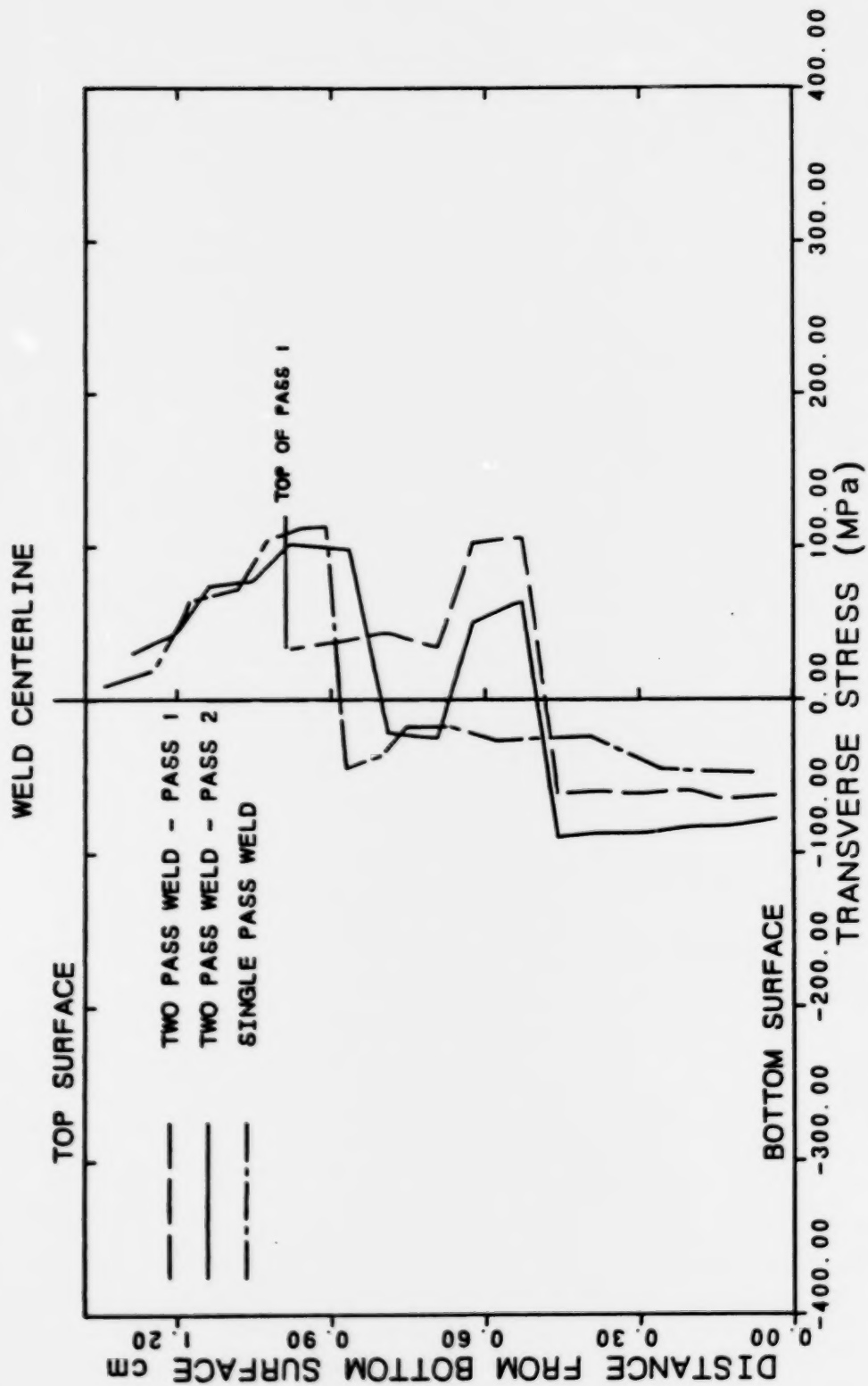


FIGURE 4.24 TRANSVERSE RESIDUAL STRESS DISTRIBUTION ALONG THE WELD CENTERLINE TWO PASSES LAID VERTICALLY

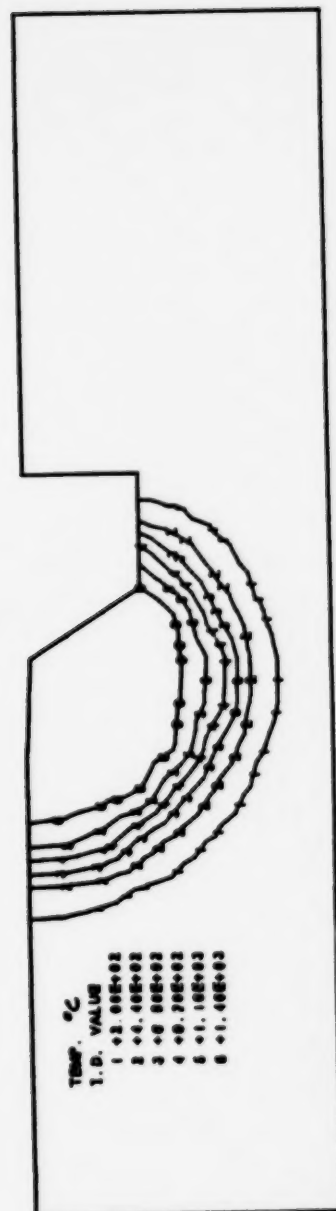


FIGURE 4.25 WELD POOL SHAPE (TWO PASSES LAID SIDE BY SIDE) - PASS 1
 STEP 3 INCREMENT 40
 ABAQUS VERSION 4.5-147



FIGURE 4.26 WELD POOL SHAPE (TWO PASSES LAID SIDE BY SIDE) - PASS 2
STEP 9 INCREMENT 40
ABAQUS VERSION 4.5-147

DISPL. FACTOR = $41.8E+00$
SOLID LINES - DISPLACED MESH
DASHED LINES - ORIGINAL MESH

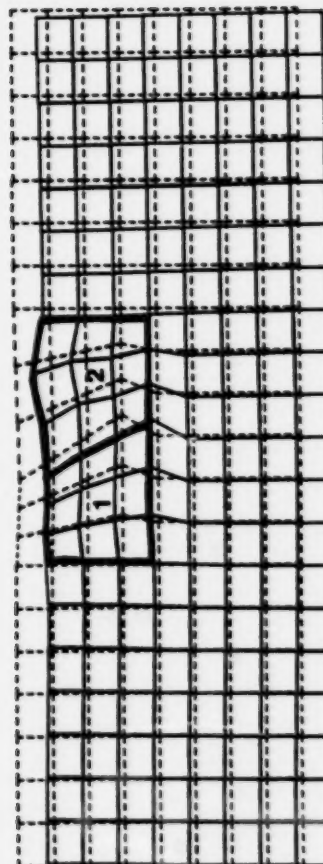


FIGURE 4.27 DISTORTED MESH PLOT (TWO PASSES LAID SIDE BY SIDE)

STEP 8 INCREMENT 2 ABAQUS VERSION 4.5-1.07

DISPL. FACTOR = +5.8E+00
 MAG. FACTOR = +5.8E+00
 SOLID LINES - DISPLACED MESH
 DASHED LINES - ORIGINAL MESH



FIGURE 4.28 DEFORMED PLATE GEOMETRY AFTER WELDING (TWO PASSES LAID SIDE BY SIDE
 STEP 8 INCREMENT 2 ABAQUS VERSION 4-3-147

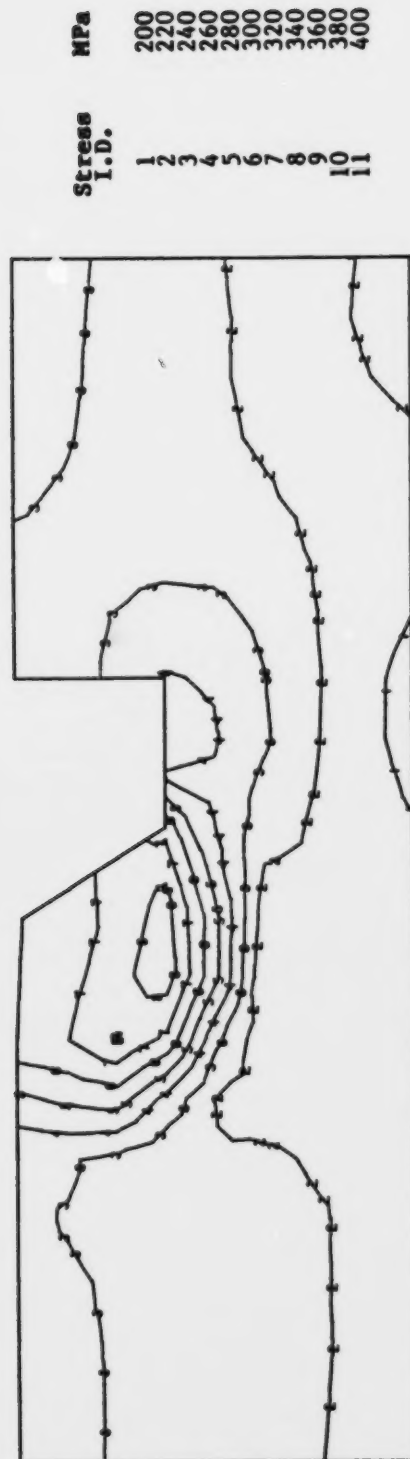


FIGURE 4.29 LONG. RESIDUAL STRESSES (TWO PASSES LAID SIDE BY SIDE) - PASS 1
 STEP 3 INCREMENT 2
 ABAQUS VERSION 4.5-147

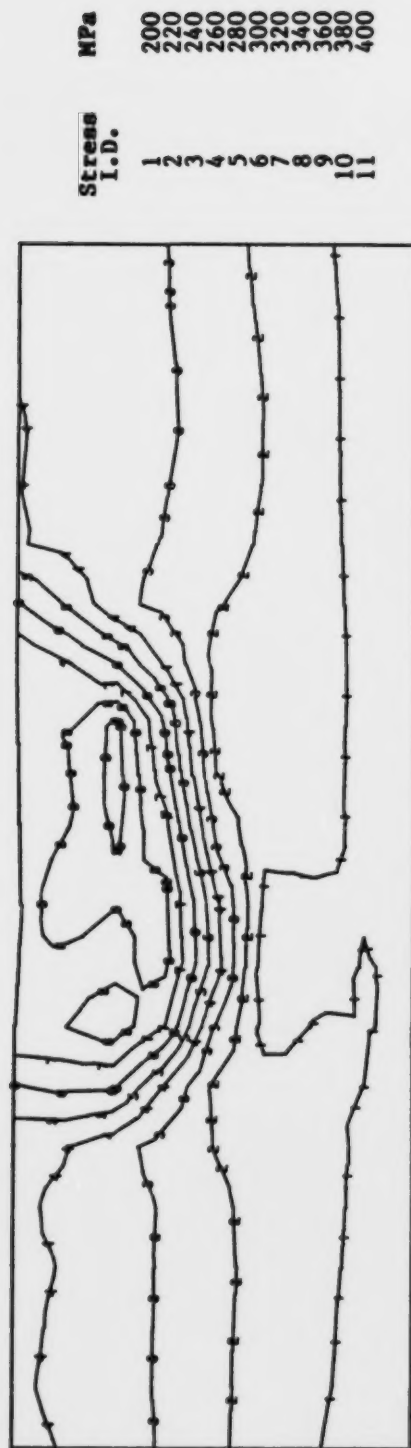


FIGURE 4.30 LONG. RESIDUAL STRESSES (TWO PASSES LAID SIDE BY SIDE) - PASS 2

ABAQUS VERSION 4.5-147

STEP 8 INCREMENT 2

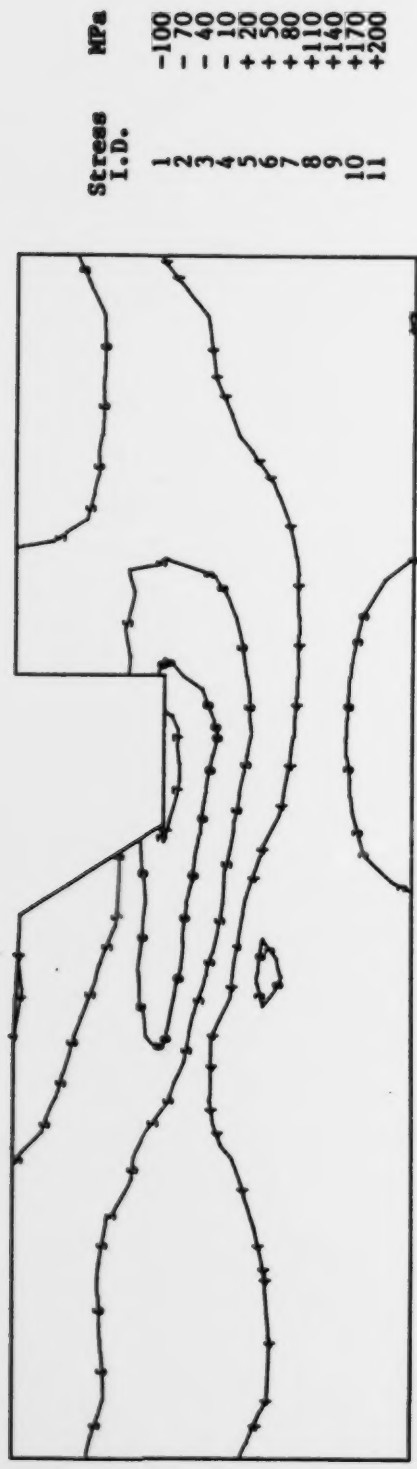


FIGURE 4.31 TRANSVERSE RESIDUAL STRESSES (TWO PASSES LAID SIDE BY SIDE) -PASS 1
STEP 3 INCREMENT 2

ABAQUS VERSION 4.3-147

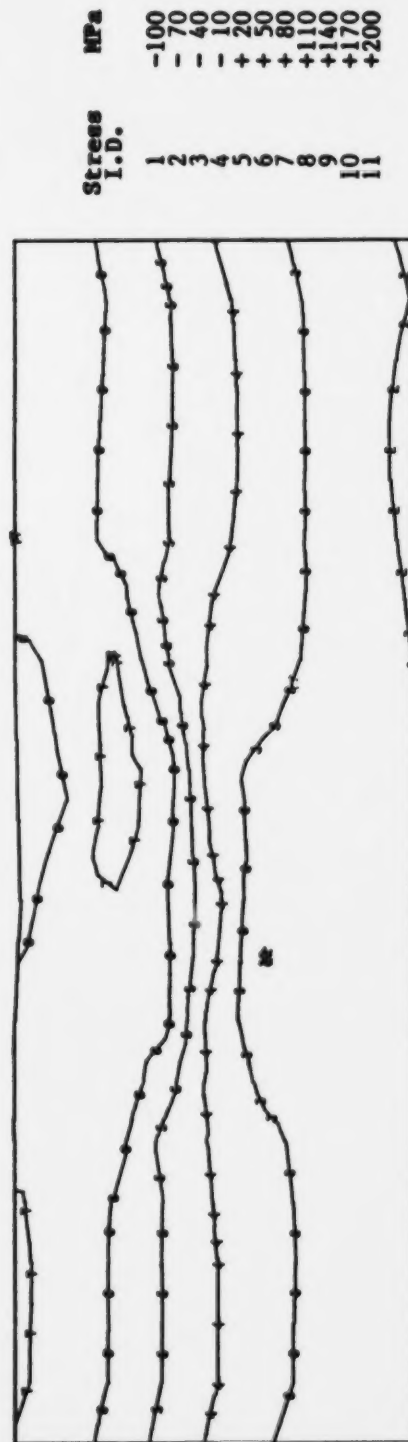


FIGURE 4.32 TRANSVERSE RESIDUAL STRESSES (TWO PASSES LAID SIDE BY SIDE) - PASS 2
STEP 8 INCREMENT 2 ABAQUS VERSION 4.5-147

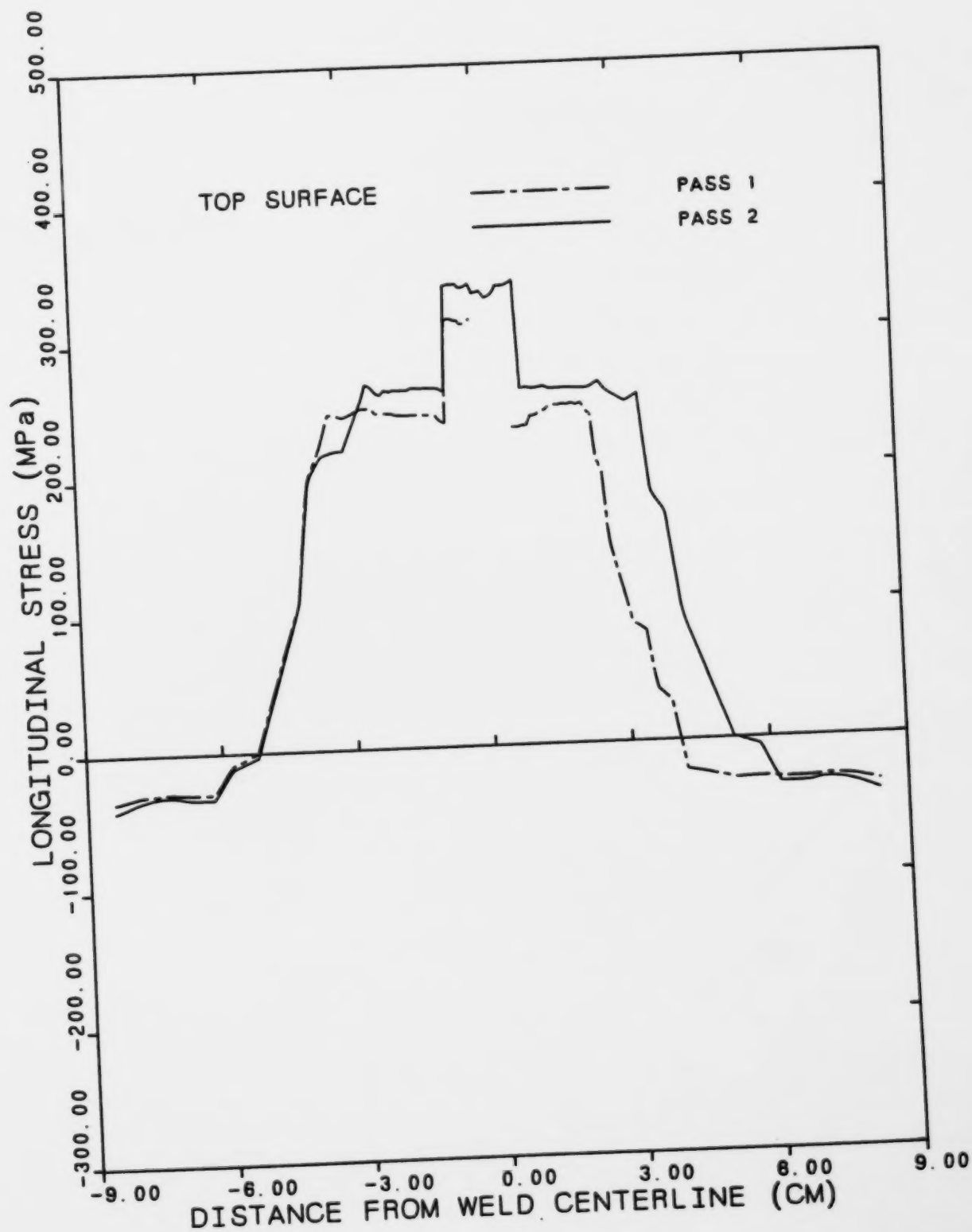


FIGURE 4.33 LONG. RESIDUAL STRESS DISTRIBUTION ON THE TOP SURFACE - TWO PASSES LAID SIDE BY SIDE

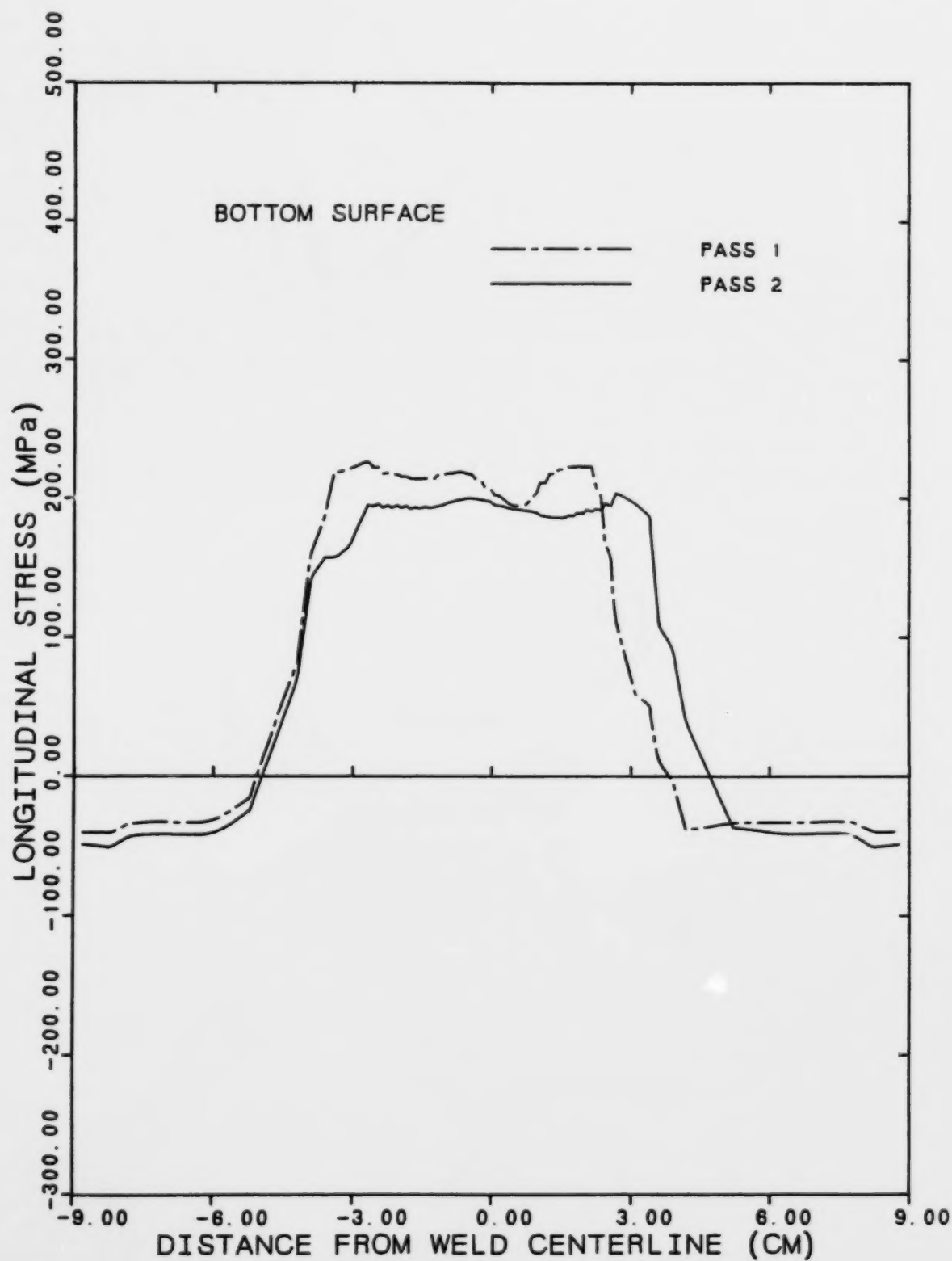


FIGURE 4.34 LONG. RESIDUAL STRESS DISTRIBUTION ON THE BOTTOM SURFACE - TWO PASSES LAID SIDE BY SIDE

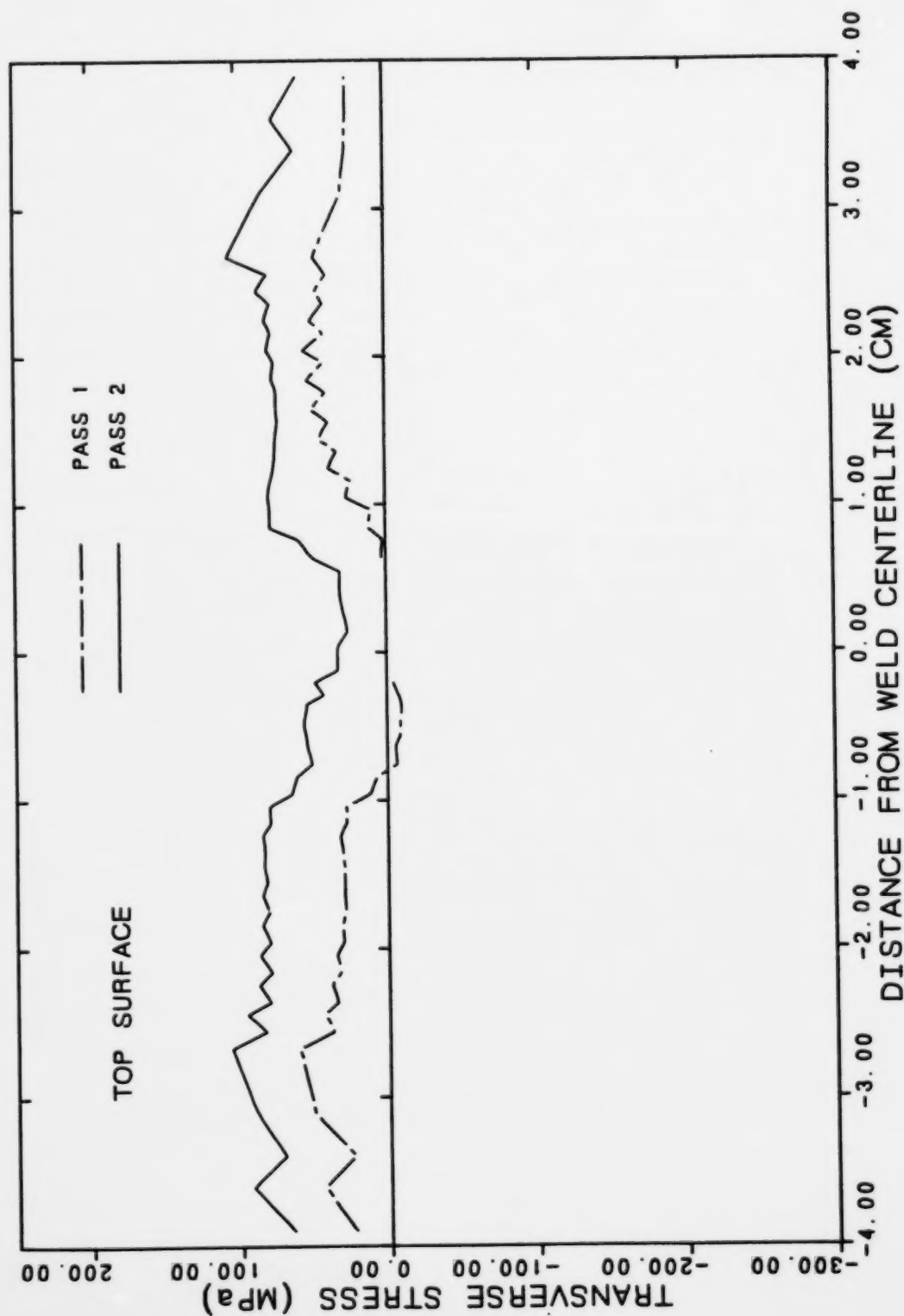


FIGURE 4.35 TRANSVERSE RESIDUAL STRESS DISTRIBUTION ON THE TOP SURFACE TWO PASSES LAID SIDE BY SIDE

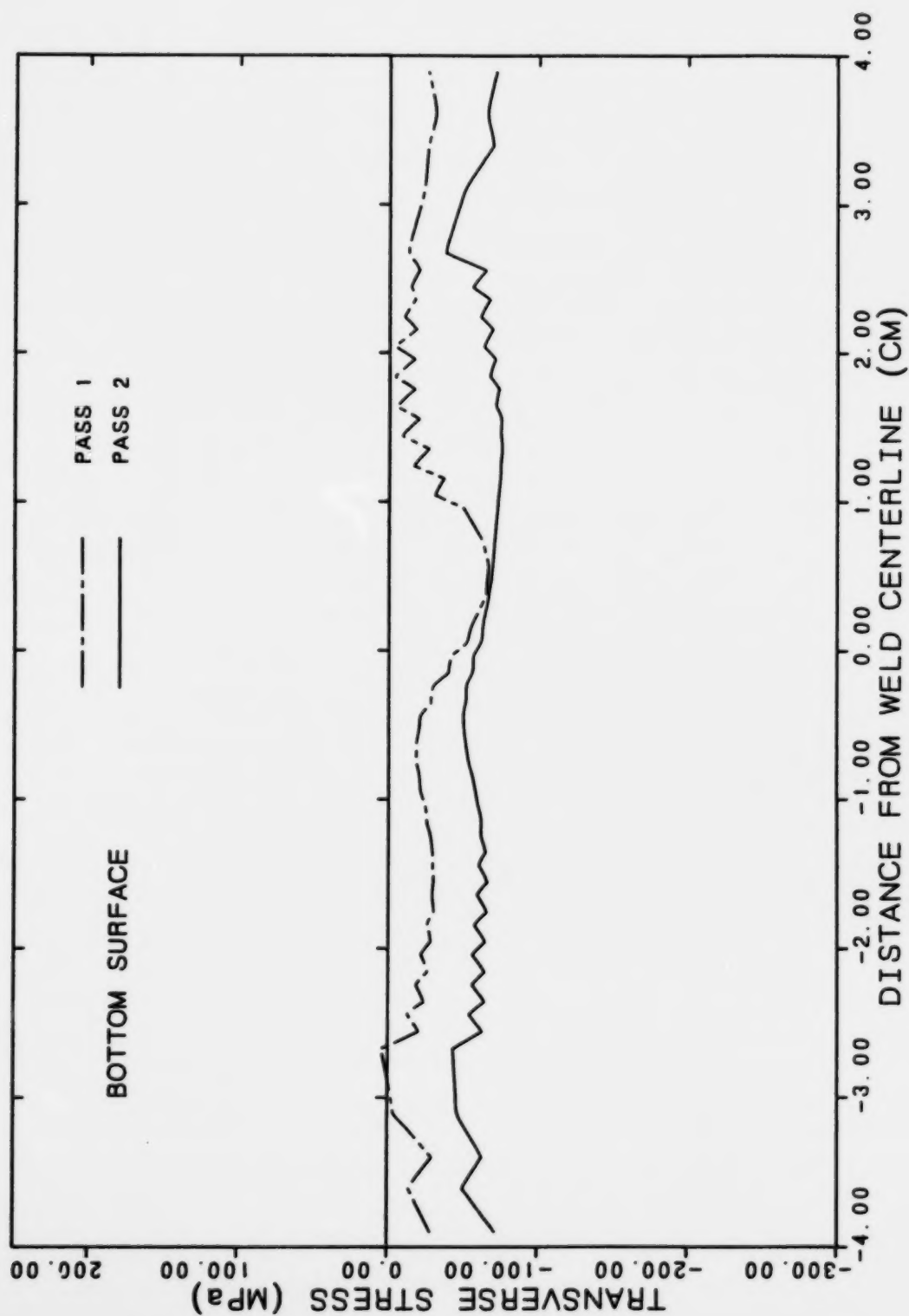


FIGURE 4.36 TRANSVERSE RESIDUAL STRESS DISTRIBUTION ON THE BOTTOM SURFACE TWO PASSES LAID SIDE BY SIDE

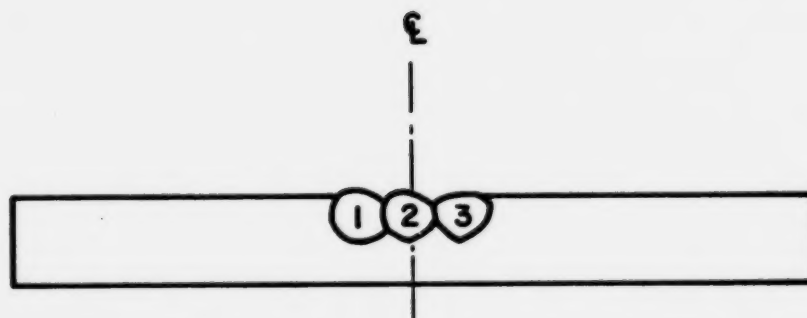


FIGURE 5.1 THREE PASS WELD - THREE PASSES LAID
SIDE BY SIDE

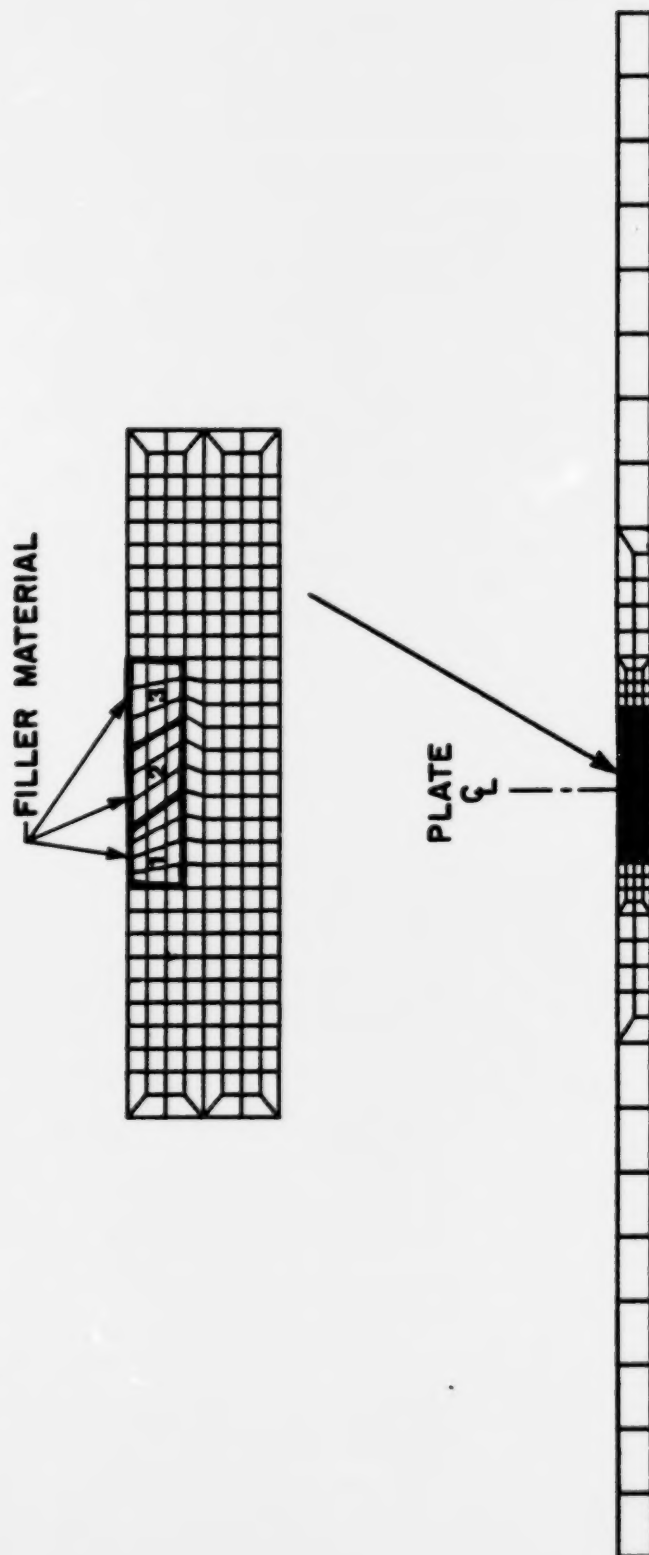


FIGURE 5.2 FINITE ELEMENT MESH - THREE PASSES LAID SIDE BY SIDE

ABAQUS VERSION 4.8-147

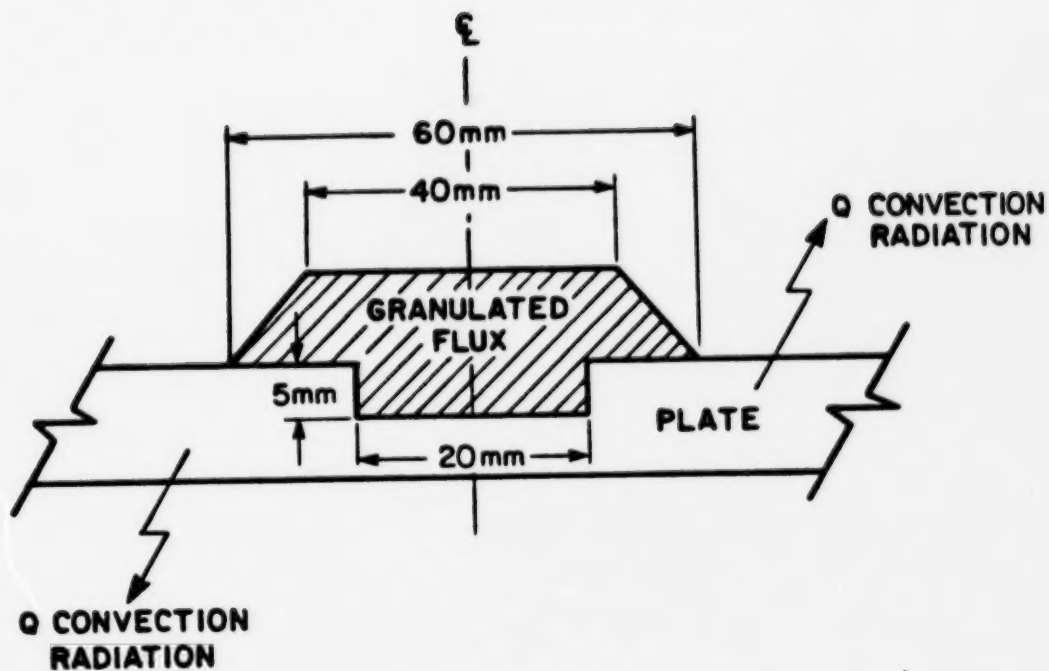


FIGURE 5.3 THERMAL BOUNDARY CONDITIONS - THREE PASSES LAID SIDE BY SIDE

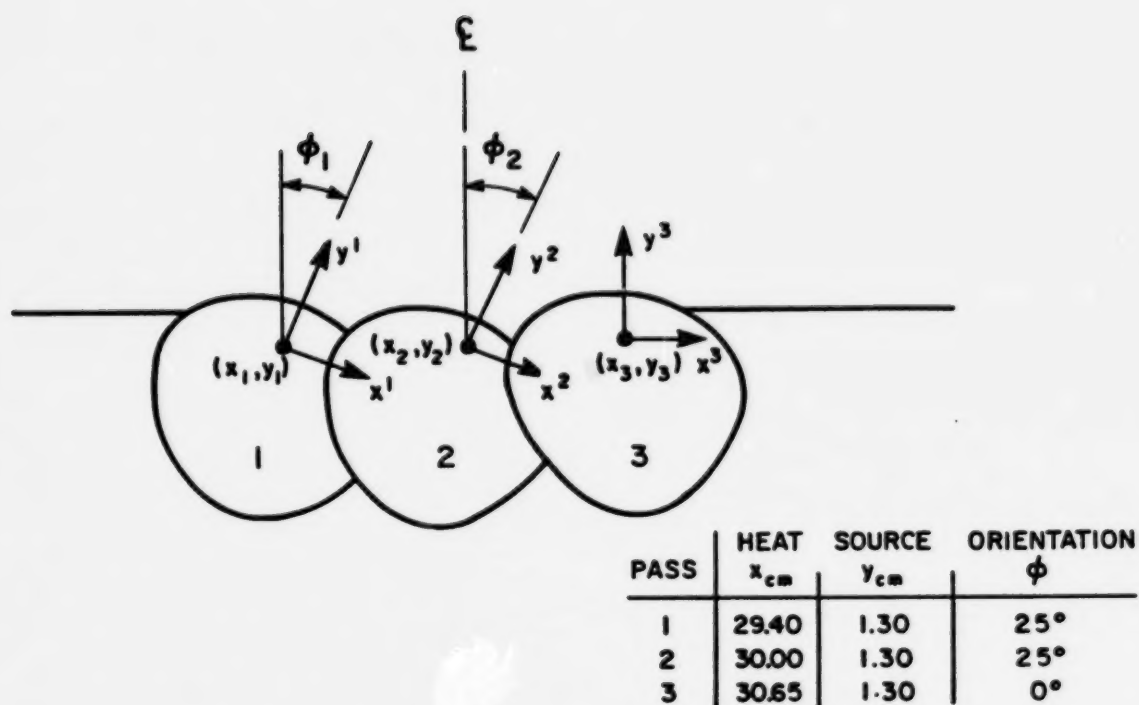


FIGURE 5.4 HEAT SOURCE LOCATION - THREE PASSES LAID SIDE BY SIDE

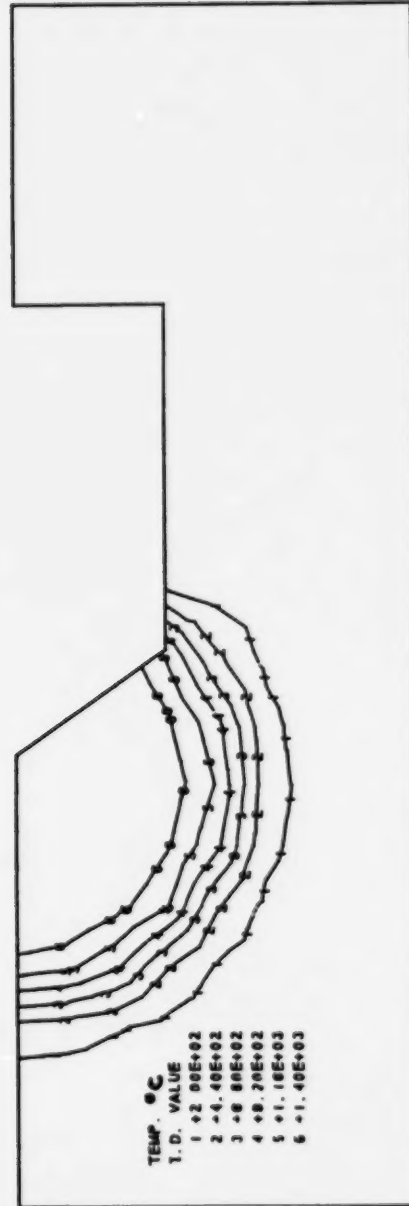


FIGURE 5.5 WELD POOL SHAPE (THREE PASSES LAID SIDE BY SIDE) - PASS 1

STEP 2 INCREMENT 10 ABAQUS VERSION 4.5-147

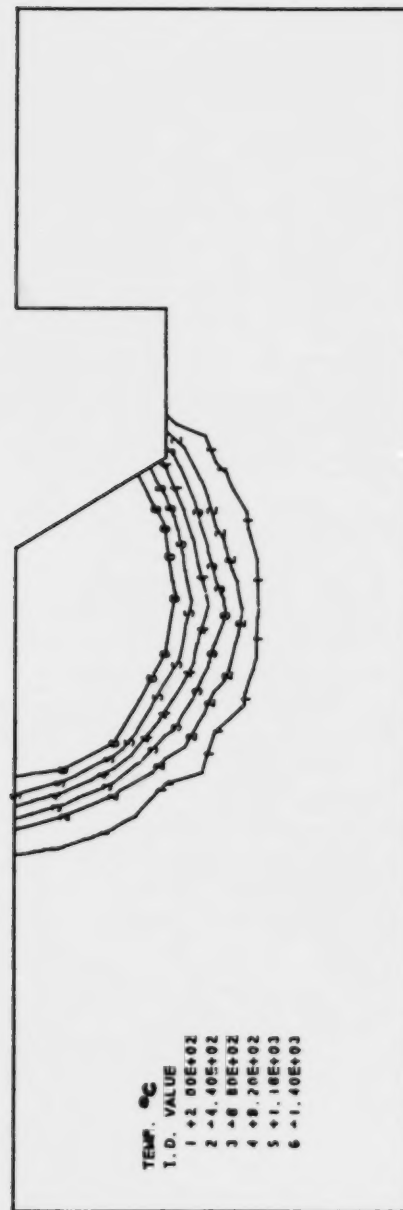


FIGURE 5.6 WELD POOL SHAPE (THREE PASSES LAID SIDE BY SIDE) - PASS 2
STEP 8 INCREMENT 30
ABAQUS VERSION 4.5-147

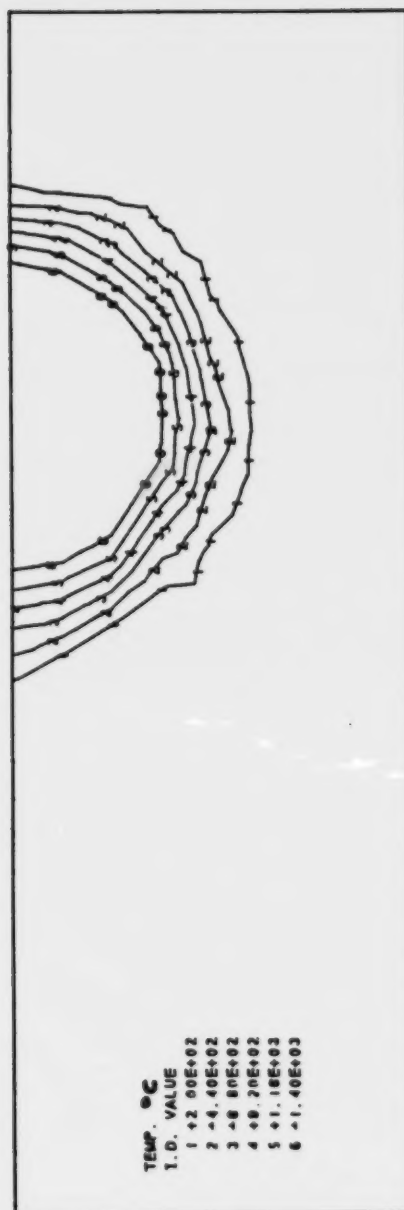


FIGURE 5.7 WELD POOL SHAPE (THREE PASSES LAID SIDE BY SIDE) - PASS 3
STEP 10 INCREMENT 30 ABAQUS VERSION 4.8-147

DISPL. FACTOR = $+1.0E+00$
 SOLID LINES - DISPLACED MESH
 DASHED LINES - ORIGINAL MESH

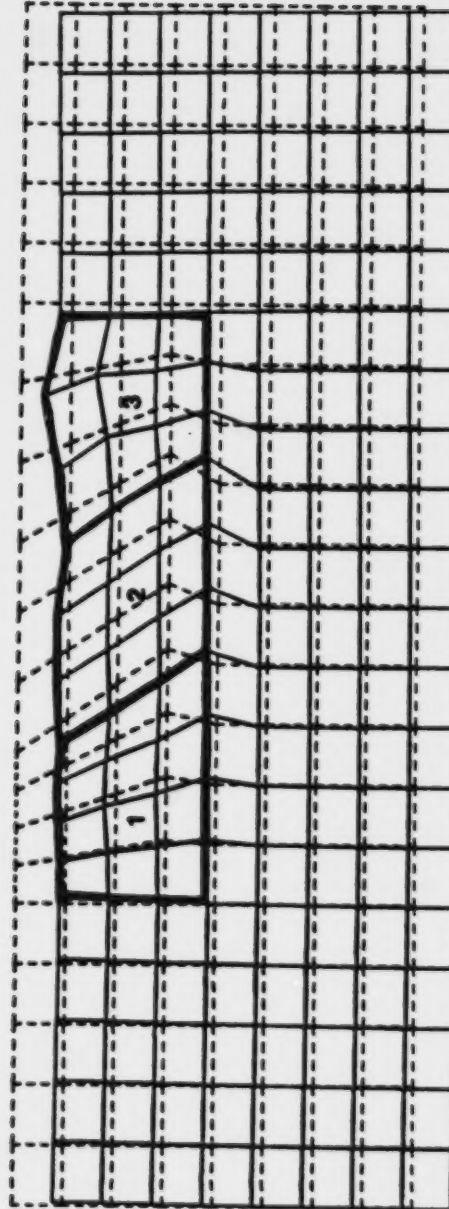


FIGURE 5.8 DISTORTED MESH PLOT - THREE PASSES LAID SIDE BY SIDE
 STEP 13 INCISEMENT 2 ABAQUS VERSION 4.8-147

DISPL. FACTOR = 0.5E+00
 MAG. FACTOR = 0.5E+00
 SOLID LINES - DISPLACED MESH
 DASHED LINES - ORIGINAL MESH



FIGURE 5.9 DEFORMED PLATE GEOMETRY AFTER WELDING -THREE PASSES LAID SIDE BY SIDE
 STEP 13 INCREMENT 2
 ABAQUS VERSION 4.5-147

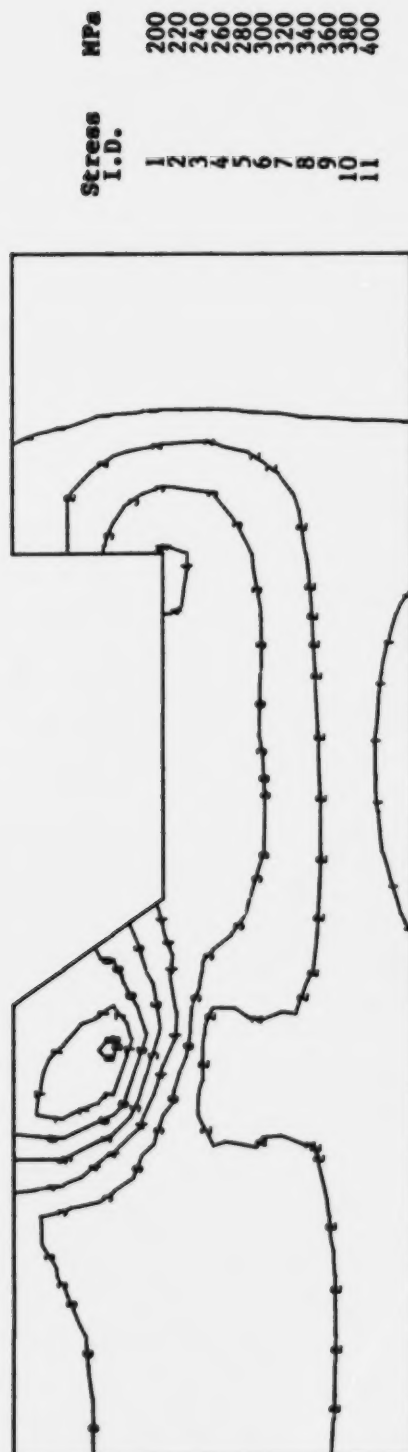


FIGURE 5.10 LONG. RESIDUAL STRESSES (THREE PASSES LAID SIDE BY SIDE) - PASS 1

STEP 3 INCREMENT 2 ABAQUS VERSION 4.3-11.7

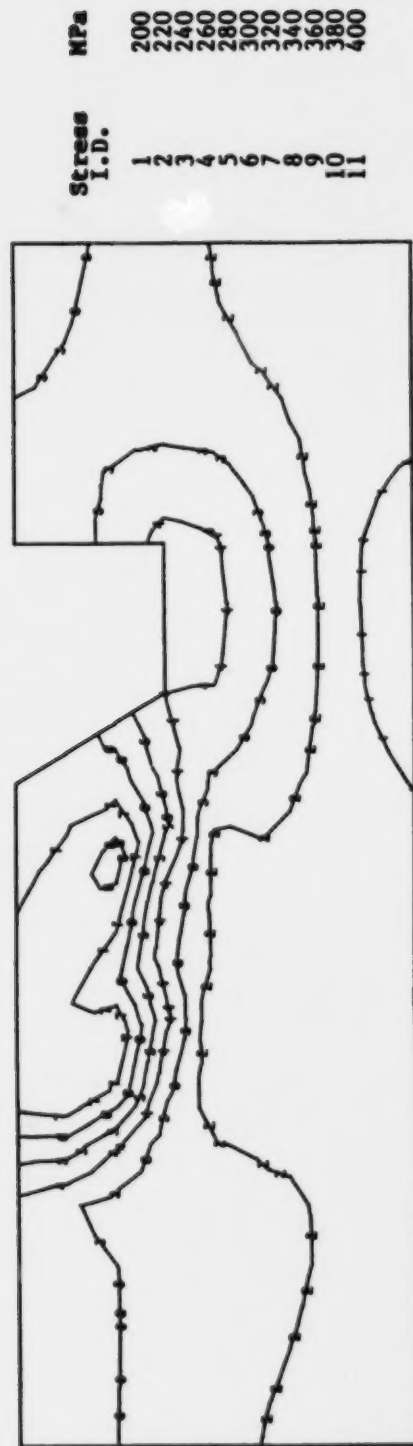


FIGURE 5.11 LONG. RESIDUAL STRESSES (THREE PASSES LAID SIDE BY SIDE) - PASS 2
STEP 8 INCREMENT 2
ABAQUS VERSION 4.5-147

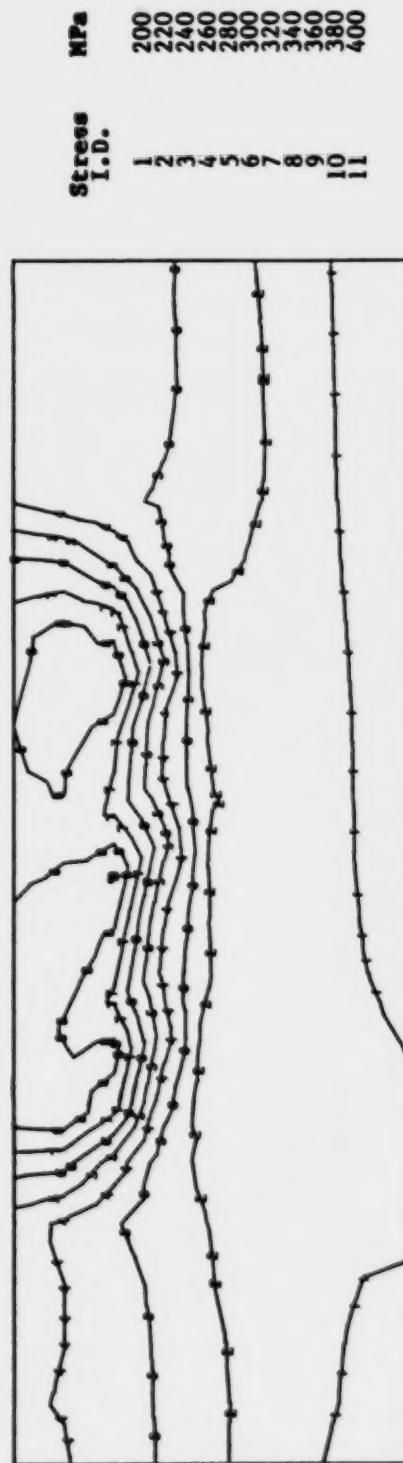


FIGURE 5.12 LONG. RESIDUAL STRESSES (THREE PASSES LAID SIDE BY SIDE) - PASS 3

STEP 13 INCREMENT 2 ABAQUS VERSION 4.5-147

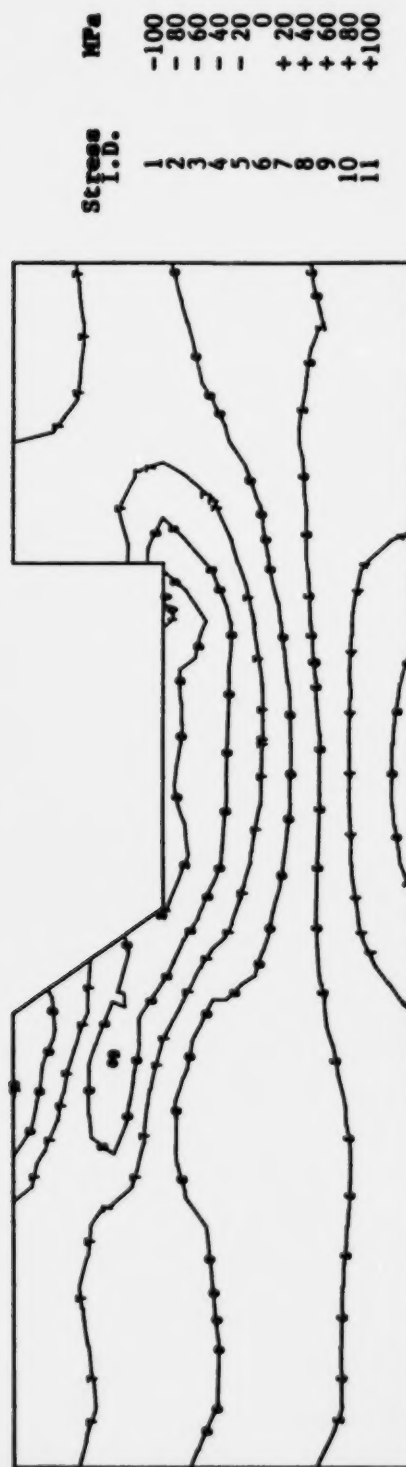


FIGURE 5.13 TRANSVERSE RESIDUAL STRESSES (THREE PASSES LAID SIDE BY SIDE - PASS 1
STEP 3 INCREMENT 2

ABAQUS VERSION 4.5-147

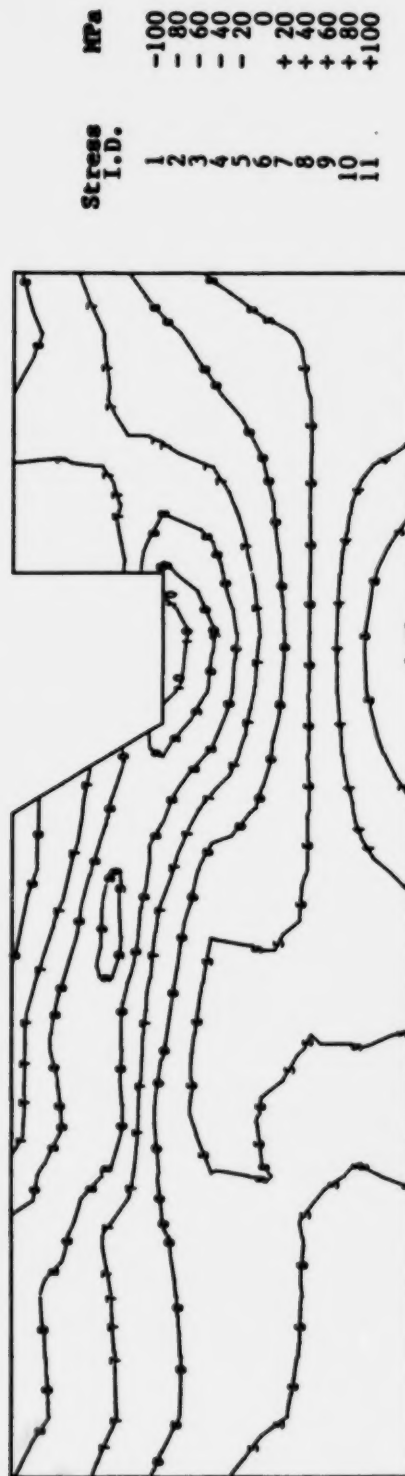


FIGURE 5.14 TRANSVERSE RESIDUAL STRESSES (THREE PASSES LAID SIDE BY SIDE) - PASS 2
 STEP 8 INCREMENT 2 ABAQUS VERSION 4.5-147

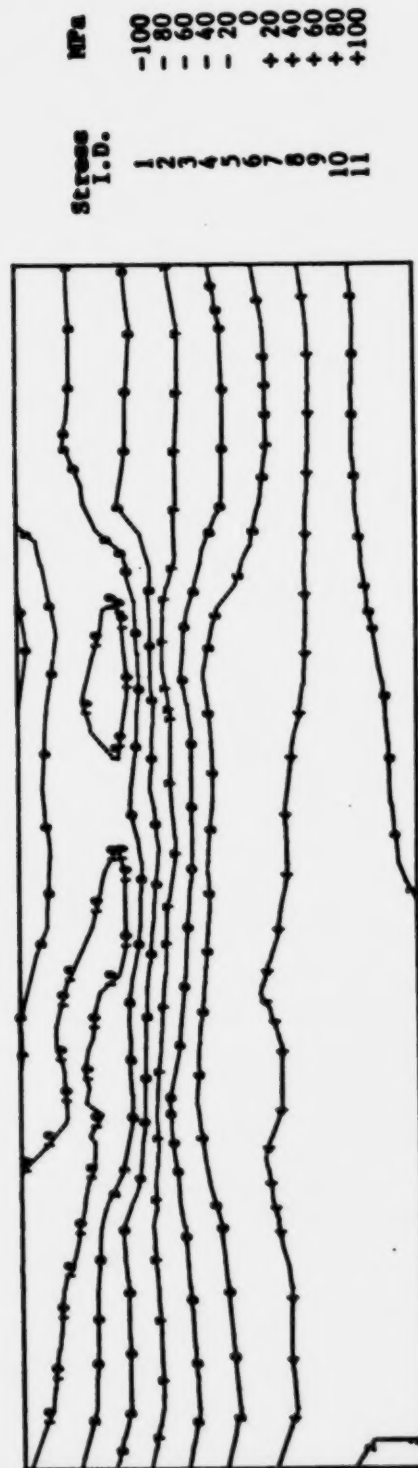


FIGURE 5.15 TRANSVERSE RESIDUAL STRESSES (THREE PASSES LAID SIDE BY SIDE) - PASS 3

STEP 12 INCISEMENT 2 ABAQUS VERSION 4.8-147

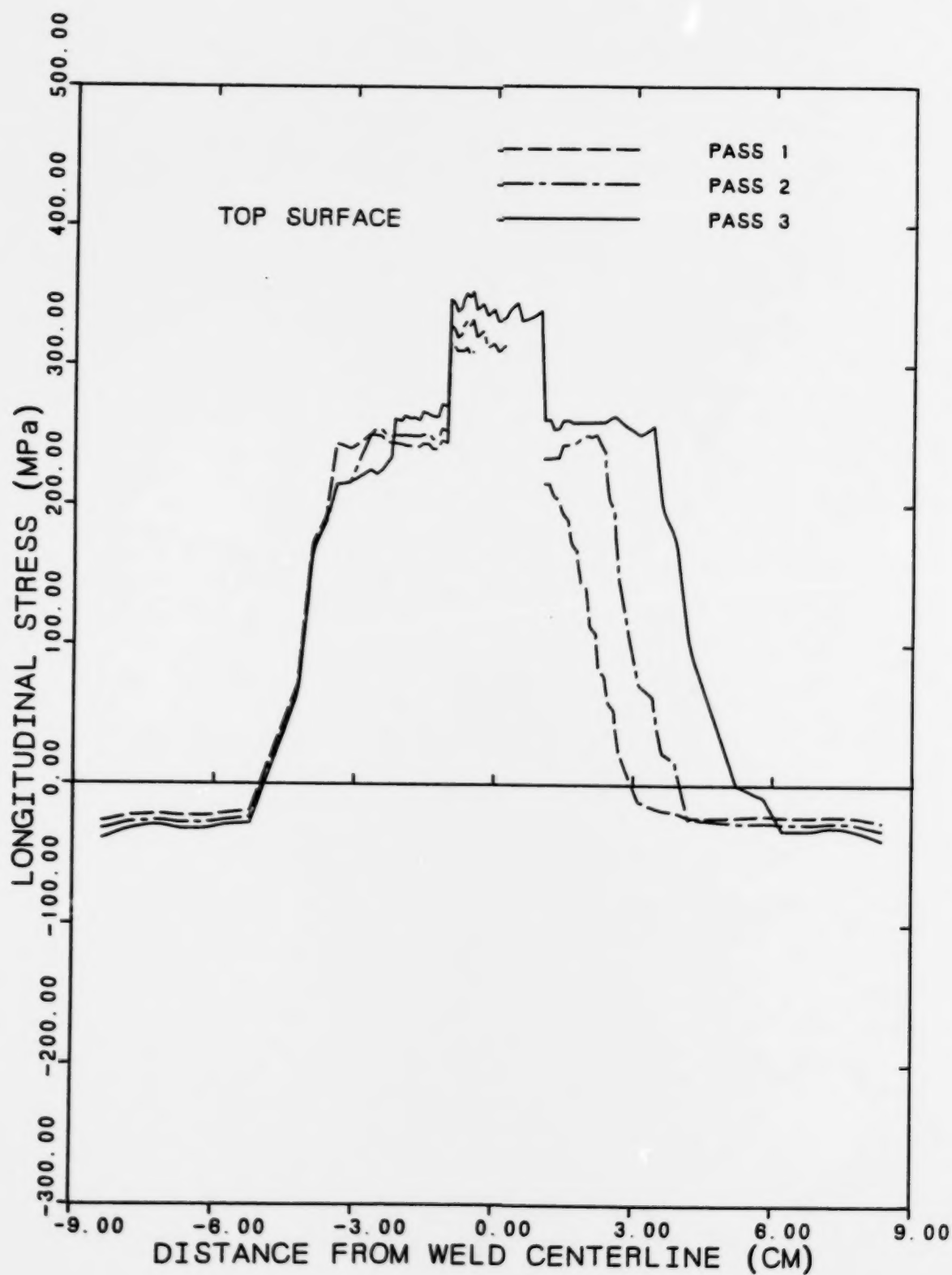


FIGURE 5.16 LONG. RESIDUAL STRESS DISTRIBUTION ON THE TOP SURFACE - THREE PASSES LAID SIDE BY SIDE

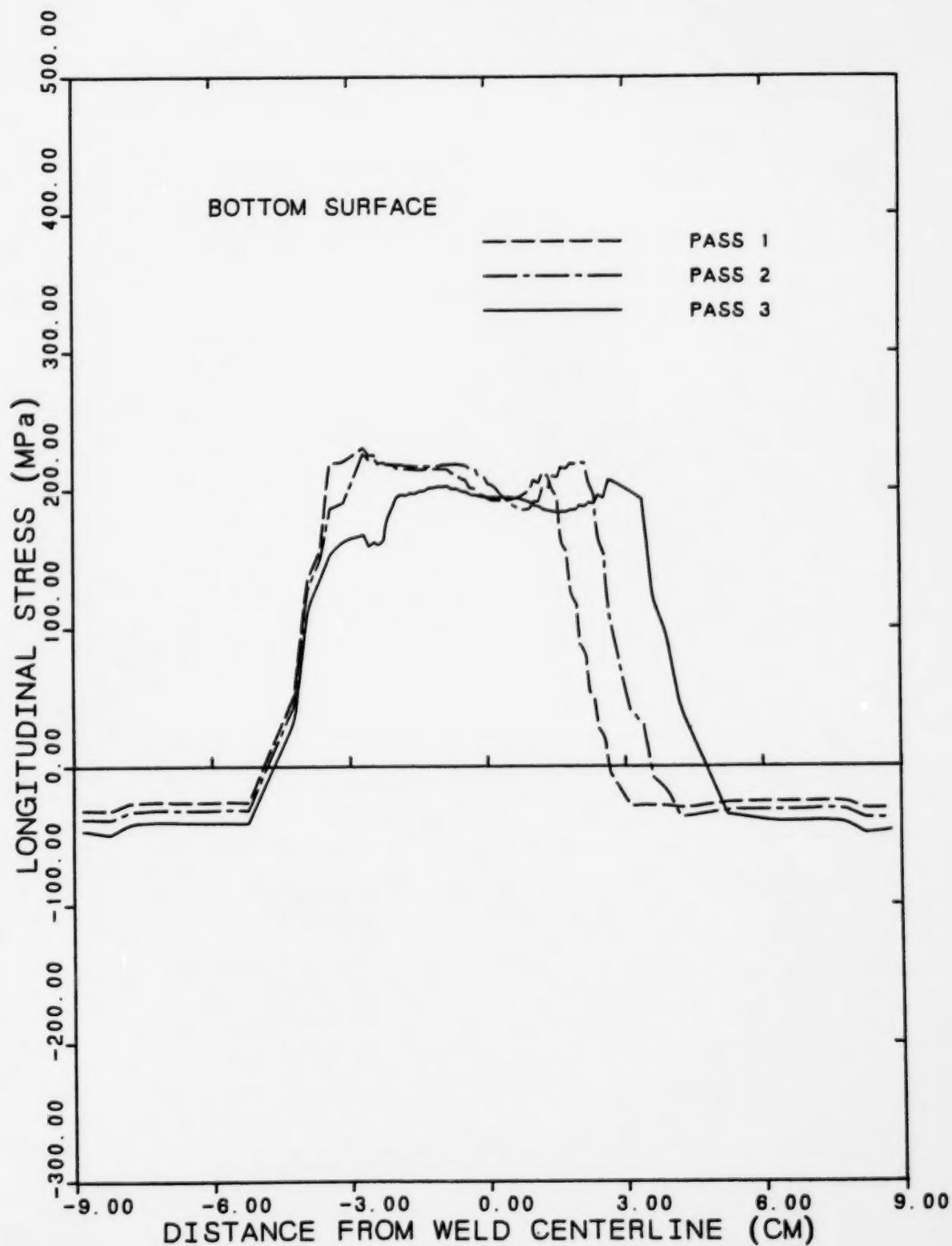


FIGURE 5.17 LONG. RESIDUAL STRESS DISTRIBUTION ON THE BOTTOM SURFACE - THREE PASSES LAID SIDE BY SIDE

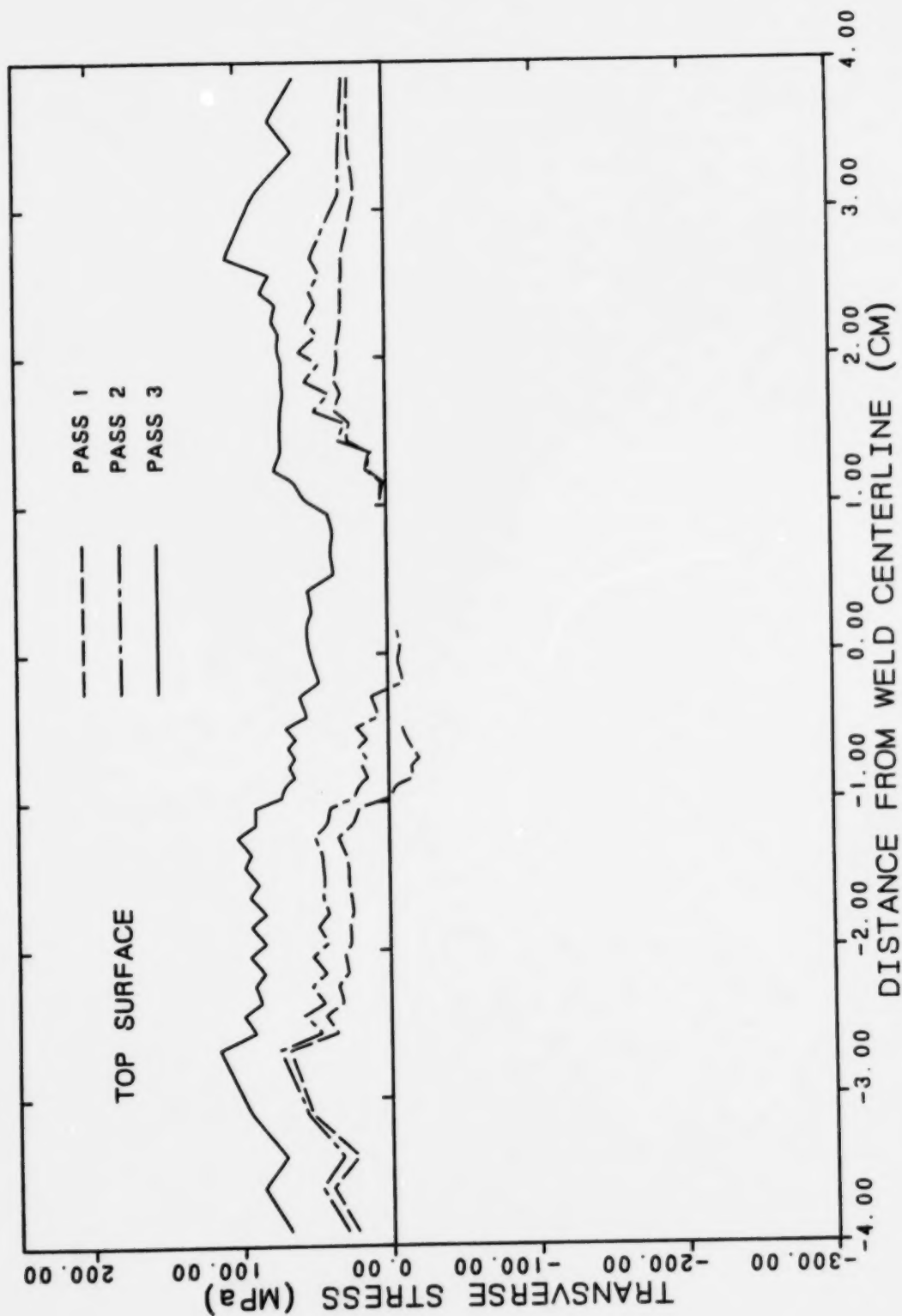


FIGURE 5.18 TRANSVERSE RESIDUAL STRESS DISTRIBUTION ON THE TOP SURFACE
THREE PASSES LAID SIDE BY SIDE

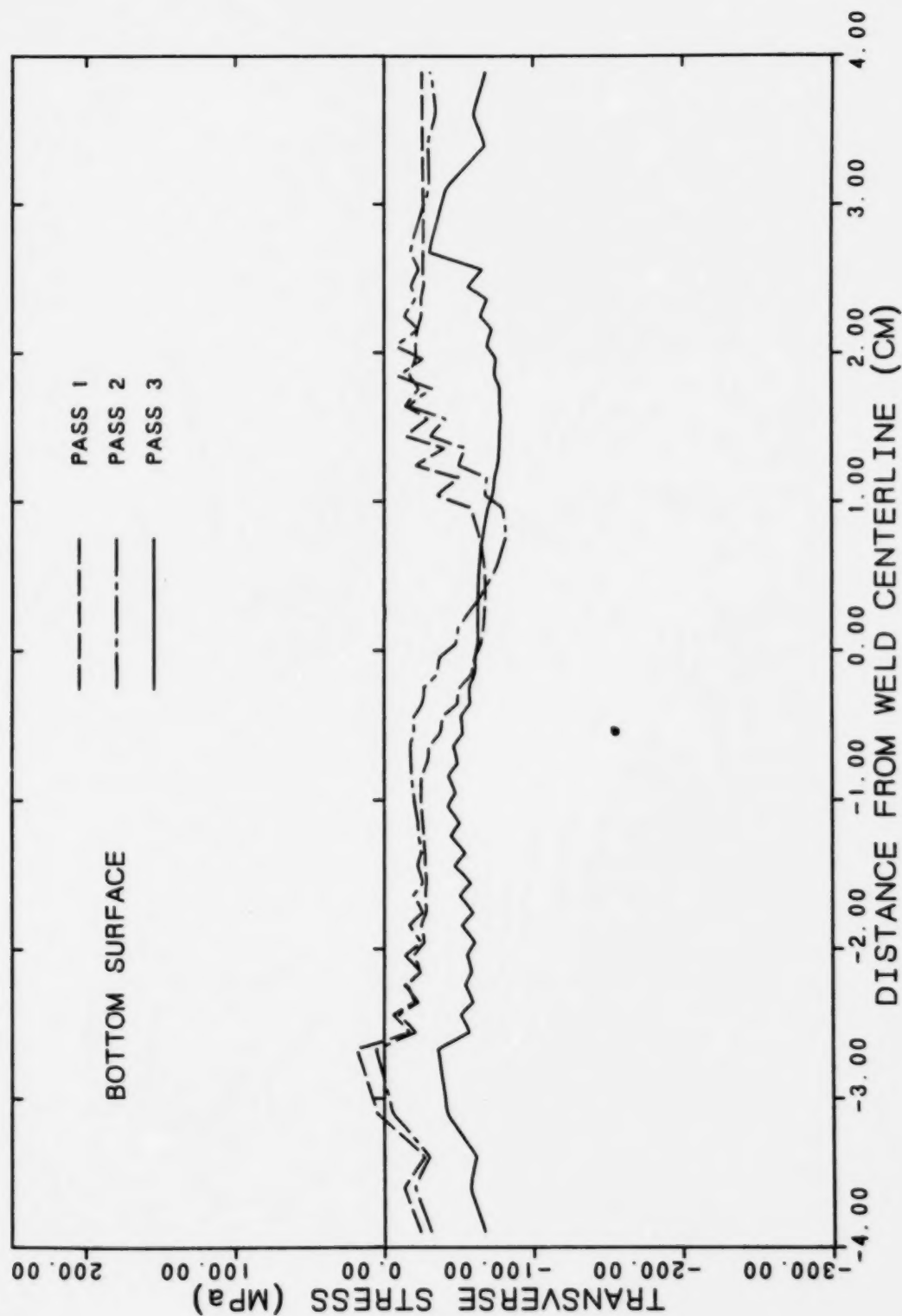


FIGURE 5.19 TRANSVERSE RESIDUAL STRESS DISTRIBUTION ON THE BOTTOM SURFACE
THREE PASSES LAID SIDE BY SIDE

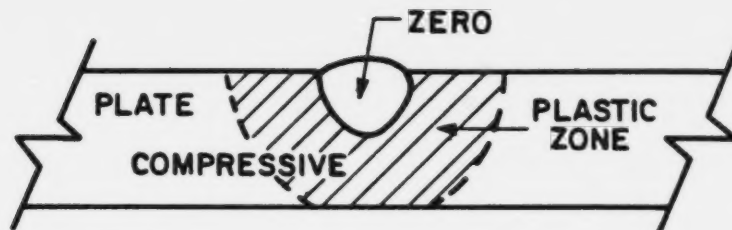


FIGURE 6.1 SKETCH OF STRESSES DISTRIBUTION BEFORE WELD METAL SOLIDIFICATION

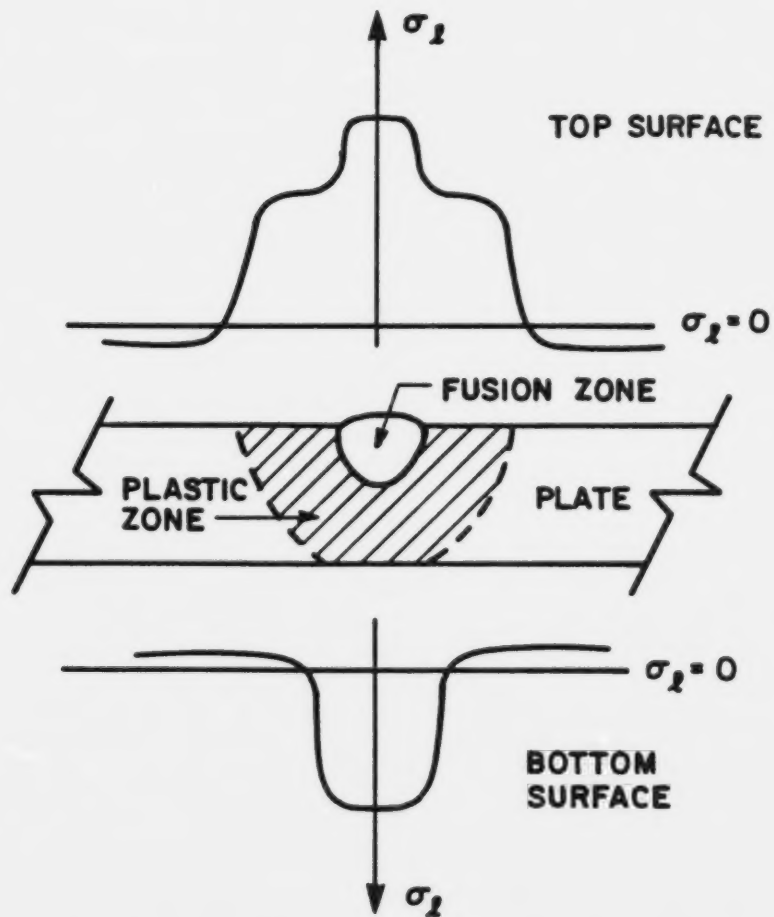


FIGURE 6.2 SKETCH OF THE LONGITUDINAL RESIDUAL STRESS

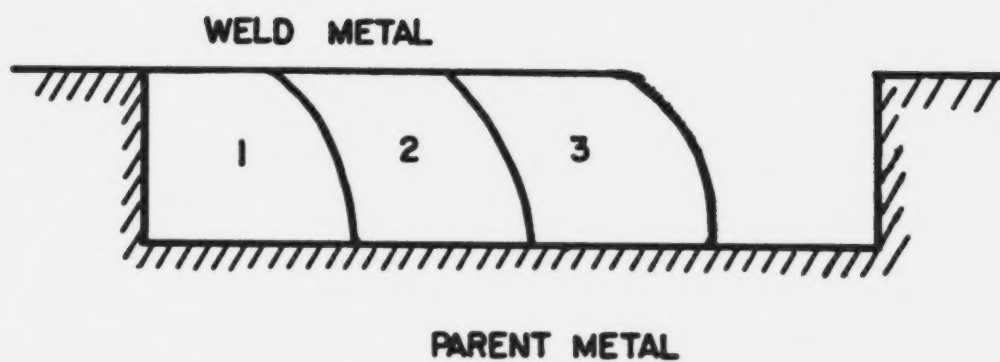
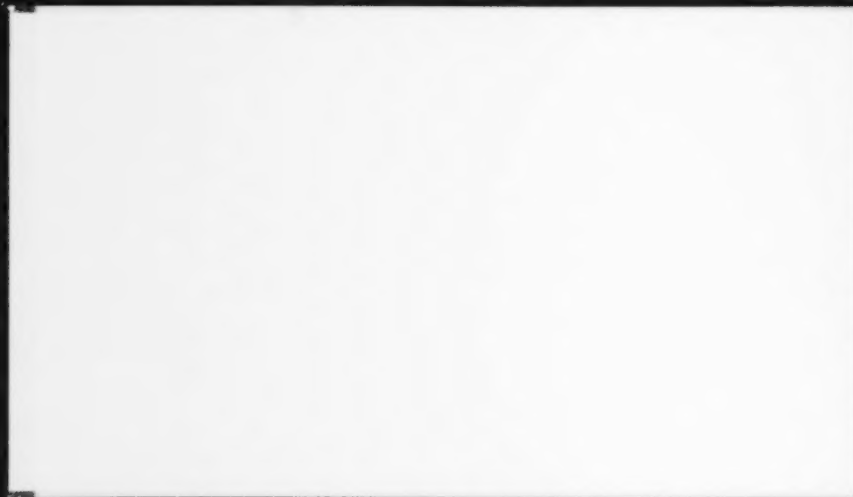


FIGURE 6.3 SKETCH OF MULTIPASS WELD LAYER

University of Waterloo

Department of

Mechanical Engineering



FINAL REPORT

(APPENDICES)

STUDY OF FINITE ELEMENT MODELLING OF

MULTIPASS WELDS

Kim C. Leung

R. J. Pick

154 pp

MICROMEDIA

AUG 15 1986

APPENDIX A

LITERATURE SURVEY

Appendix A

LITERATURE SURVEY

A.1 INTRODUCTION

In the determination of weld residual stress the calculation of the temperature history can be uncoupled from the calculation of the stress history in the weld. The necessary conditions for making such an assumption, described by Hibbitt [A.1], are that the dimensional changes during welding have little effect on the implementation of thermal boundary conditions and the strain rate effect on entropy production is negligible as compared to the temperature rate term. This uncoupled approach to the welding problem has been adopted by most researchers because it simplifies the analyses considerably. A weak thermo-mechanical coupling strategy was attempted by Argyris [A.2]. This approach requires the two field problems to be solved sequentially within a staggered solution scheme, so that the strain rate effect on entropy from a previous stress analysis time step will be accounted for during the current step of the thermal analysis. However no comparison was made to demonstrate its improvement in solution accuracy over the uncoupled approach. Fully coupled analysis is also possible [A.3] but is considered impractical because of the high computing cost involved.

In the following sections the analysis requirements for the heat transfer and stress problems during welding based on the uncoupled approach will be discussed. This is followed by a brief review of the literature of both analytical and numerical calculation techniques

related to the prediction of weld residual stresses. Techniques for modelling multi-pass welds will also be reviewed.

A.2 HEAT TRANSFER ANALYSIS

Most of the physical phenomena accompanying welding such as metallurgical changes, residual stresses and distortion have their origin in the uneven temperature distribution and the fast cooling rates that occur during a welding process. Therefore an accurate prediction of the transient temperature distribution and cooling rate are a prerequisite for accurate prediction of weld residual stress.

The heat flow in a weldment is governed by a three dimensional transient-heat conduction equation, (see figure A.1)

$$\frac{\partial}{\partial x} \left(k \frac{\partial T}{\partial x} \right) + \frac{\partial}{\partial y} \left(k \frac{\partial T}{\partial y} \right) + \frac{\partial}{\partial z} \left(k \frac{\partial T}{\partial z} \right) = \rho c \frac{\partial T}{\partial t} \quad (\text{A.1})$$

where k = thermal conductivity
 ρ = density
 c = specific heat
 t = time
 T = temperature
 (x,y,z) = cartesian coordinates

which can be solved if the boundary conditions and the thermal properties are known. The net heat flow into a weldment consists of the power supplied to the weld head less the heat escaping through the electrode and flux. The net heat flow out of the weldment occurs by means of surface convection and radiation which are both temperature dependent. Thermal properties such as density, specific heat and thermal conductivity required in the heat transfer calculations are also functions of temperature. These temperature dependent properties and surface conditions make the problem highly non-linear. This is further

complicated by the convective heat transfer in the molten weld pool and the absorption and liberation of latent heat accompanying phase changes which must be accounted for in heat flow calculations.

The first analytical solution to the heat flow problem during welding was developed by Rosenthal [A.4] in 1941. To arrive at the solution, Rosenthal assumes that the heat flow in a weldment has reached a quasi-stationary state, so that an observer would notice no change in the temperature field moving with the electrode. To further simplify the problem he also assumed constant thermal properties and neglected convection, radiation and latent heat effects. Based on these assumptions Rosenthal obtained two solutions, one using a point heat source model for an infinite three dimensional medium and the other using a line heat source model for an infinite two dimensional plate. These solutions generally yield good results far away from the fusion zone but not in the vicinity of the weld pool.

Many modifications to Rosenthal's fundamental solutions have been made to overcome some of the original assumptions. Rosenthal [A.5] has included surface heat losses in the line heat source model for thin plates. For thin plates convection and radiation losses are significant compared to conduction heat flow within the weldment boundaries. For finite size plates bounded by adiabatic surfaces Rosenthal [A.4] suggested using a "mirror image method" to compensate for heat flows through these surfaces.

To account for temperature dependent materials properties Masubuchi et.al. [A.6] adopted an iterative solution scheme. Starting with an initial guess of temperature and corresponding material properties, Masubuchi's iteration scheme uses material properties updated after each

iteration until temperatures converge. However there is no guarantee that the technique will converge to the correct solution. Grosh et.al. [A.7] developed a two dimensional solution assuming a linear temperature variation of material properties. Unfortunately this assumption of material behaviour only applies to some aluminum alloys. Kazimirov et.al. [A.8] developed a similar solution which allows for both linear variation of material properties as well as surface heat losses to the environment.

In an attempt to improve the estimation of the temperature distribution in the vicinity of the weld, Rykalín [A.9,A.10] has included into Rosenthal's solution a finite size heat source rather than a point or line heat source. Papazoglou [A.11] advanced this further by developing a solution which assumed a 2D skewed normally distributed surface heat source and linear temperature variation of thermal properties. This is considered the "state of the art" analytical solution to the thermal problem for single pass welds.

The analytical solutions described above are unable to predict accurate temperatures particularly in the vicinity of the weld. This is mainly due to the highly non-linear nature of the problem, as well as neglect of latent heat effects, convective mechanism in the weld pool and the complex weld geometry of multi-pass weldments. Nevertheless such closed form solutions are very useful tools for parametric analysis because of the low cost involved. But for the prediction of residual stresses where a more accurate description of the temperature is essential, a numerical approach to the thermal problem is more appropriate.

Two numerical methods normally considered for the solution of the highly non-linear welding heat transfer problem are the finite difference (FDM) and finite element (FEM) method. The finite element method is the most commonly used technique because of its ability to model very complex joint geometries and thermal boundary conditions in the weld vicinity. In addition since the residual stress analysis commonly uses the finite element method it is convenient to transfer finite element heat flow results to this type of stress analysis. For this study the finite element method has been adopted. However since similar considerations are necessary whether the finite element method or the finite difference method is used the following literature review will consider both methods. The discussion will not be focused on either numerical method, but rather on the techniques of modelling various thermal phenomena occurring during a welding process.

A.2.1 Dimensionality of the Problem

The heat flow in a weldment is spatially three dimensional. Paley and Hibbert [A.12] have developed a three-dimensional heat flow model based on the finite difference method. Kou and Le [A.13] utilized a similar 3D finite difference model in the study of heat flow and solidification during the GTA welding of aluminum plates. Although the finite element method is capable of handling similar three dimensional analyses, Muraki [A.14,A15] and Nishida [A.16] have found that the computing cost is prohibitively high. Therefore most researchers using the finite element method have reduced the problem spatially to two dimensions by ignoring the heat flow in the welding direction. This simplification is acceptable provided that the welding speed is high compared to the material diffusion rate and that a quasi-stationary state is

established around the welding electrode. Andersson [A.17] studied the effect of ignoring the third dimension heat flow based on analytical solutions and concluded that the error is minimal for very high speed welding except in the vicinity of the heat source. For lower welding speeds Friedman [A.18] suggested a correction technique which approximates the heat flow in the welding direction as an internal heat generation term in the finite element formulation.

In general the literature indicates that a two dimensional finite element analysis with proper compensation for the heat flow in the third dimension is capable of simulating the actual heat transfer in the weldment without significant error if the analysis is made at locations far from the start and end of the weld. Masubuchi [A.6] suggests that one should consider the midpoint of a weld 18 inches or longer to neglect end effects.

A.2.2 Thermal Properties

The thermal properties required for the calculation of temperature during welding are thermal conductivity (k), specific heat (c), density (ρ), latent heat (H^*) and transformation temperatures.

The properties k , ρ and c changes considerably over the wide range of temperatures that a material is subjected to during welding. A survey of the literature reveals that the material data of specific compositions is seldom available. To overcome this difficulty, most researchers assume properties for a specific material to be similar to those available for other materials of similar composition. However most of the available data is only for lower temperature ranges making it necessary to estimate high temperature data through extrapolation.

For the solid to liquid phase transformation, the transformation temperature range is usually available in the literature while the corresponding latent heat of fusion is seldom available except for some common materials. Therefore most researchers have either ignored the latent heat of fusion in their analysis or approximated it based on the latent heats of fusion of individual elements in the material.

Latent heat effects due to allotropic transformations are often included in the specific heat data. When using this modified specific heat in the thermal analysis, latent heat effects can be accounted for if an enthalpy formulation is used.

There is no doubt that the above approximations and extrapolations will lead to errors in the solution. However unless one conducts their own experimental investigation of material properties, this "best estimate" approach appears to be the only alternative.

A.2.3 Surface Heat Losses

On the exposed surface of the weldment, heat exchange takes place with the environment by means of convection and radiation. Radiation dominates at high temperatures during the initial heating stage, while free convection dominates at low temperatures during the cooling period.

Radiation is modelled by the Stefan-Boltzman power law which requires knowledge of the surface emissivity. The surface emissivity of a material can vary between 0.0 and 1.0 depending on the surface temperature, surface roughness and the extent of oxidation. Papazoglou [A.11] in a study of GMA welding of HY 130 steel assumed a constant emissivity of 0.8. Sparrow [A.19] suggested that for steel which has seen repeated heating and cooling a constant emissivity of 0.6 should be used. Argyris

[A.20] in a study of arc welding of mild steel has assumed a temperature dependent emissivity which varies from 0.05 at room temperature to 1.0 at 600°C due to oxidation and then drops to 0.6 at the melting temperature.

Convective heat loss is modelled by the Newton's cooling law which requires a knowledge of the surface heat transfer coefficient, h_c . This coefficient is both temperature and process dependent making it very difficult to determine. Hibbitt [A.21] in a study of GMA welds assumed a constant h_c of $62 \text{ W/m}^2\text{K}$ during the cool down period, but assumed a h_c value of $44 \text{ W/m}^2\text{K}$ for the top surface of the plate during welding to account for the forced convection due to shielded gas flow. Papazoglou [A.11] assumed a temperature dependent h_c for GMA welding using values based on Tsai's [A.22] empirical study. Argyris [A.20] estimated the temperature dependent coefficient h_c based on an empirical study of isothermal surfaces. However this tends to under estimate the actual h_c value because the weldment surfaces are non-isothermal.

A.2.4 Convection in the Weld Pool

The thermal energy deposited into a weld pool is transported to other parts of the weldment by means of conduction and convection. Convection in the weld pool is a very complex phenomena influenced by the electromagnetic force, plasma jet force, and the surface tension of the molten metal during welding. In theory it is possible to conduct a full convection-diffusion type of heat transfer analysis. However because of the complexity this type of analysis is seldom performed except for a simple cases such as laser welding [A.23], in which convection in the weld pool is influenced by surface tension only.

A more tractable method of handling the problem is to ignore the physical phenomena occurring inside the weld pool. The problem is then divided into two parts, the first part estimates the weld pool shape by measuring the weld width and the ripple length, while the second part calculates the temperature distribution in the solid material using the melting temperature as a boundary condition. Investigators such as Pavelic [A.24] and Tsai [A.22] have shown good correlation between experiments and numerical results using this technique. The advantage of this method is that the net heat input to the weldment is not required and the latent heat of fusion can be ignored in the analysis.

The final and most simple method is to simulate convection in the weld puddle using an effective conductivity an order of magnitude higher than its value at the melting temperature. Brody [A.25], Papazoglou [A.11] and Friedman [A.26] have used this approach. However this approach fails to include convective heat flow in the welding direction in a simplified 2D analysis.

A.2.5 Heat Input Model

The most critical input data required for the welding heat flow analysis is the actual power, Q , that enters the weldment. This power is usually expressed in terms of the voltage, V , and current, I , supplied to the welding electrode as,

$$Q = \eta V I \quad (A.2)$$

where η is the arc efficiency representing the ratio of the actual power entered the weldment to the power supplied to the arc. This parameter is dependent on the welding process, power supply and the type of material welded. Christensen [A.27], Rykalin [A.28] and Ghent [A.29]

have experimentally measured arc efficiencies of various welding processes and materials and reported a wide range of results (see figure A.2). A brief study conducted by the authors [A.30] has shown that the peak temperatures at locations around the weld pool change approximately in proportion to the change in η . This illustrates the importance of the value of arc efficiency in calculating accurate temperature distributions in weldments. In order to ensure accurate prediction of temperatures, many researchers adopt a trial and error approach whereby the arc efficiency parameter is adjusted until a reasonable temperature distribution is obtained compared to experiments.

Of equal importance to the actual power supplied to the weldment is the distribution of power. This distribution will greatly affect the weld pool shape and hence the temperature distribution in the vicinity of the weld. Pavelic [A.24] first proposed that the power source should be a circular Gaussian distribution of thermal flux deposited on the surface of the weldment. This circular heat source was used by Kou [A.13] in a 3D GTA weld analysis and later modified by Friedman [A.31] so that it could be applied to a 2D analysis. Another model is to distribute the power uniformly over the fusion zone elements as the welding arc passes. This approach was first used by Paley [A.12] in a finite difference analysis. He found that the solution tended to over estimate the fusion zone size. This suggests that a Gaussian type of power distribution within the fusion zone is more appropriate. Goldak [A.32] has developed such a distribution model by assuming a Gaussian heat flux distribution in the weld pool simulated by two ellipsoids (see figure A.3). This model appears to produce the most accurate simulation of the temperature distribution in the fusion zone particularly in 2D

analyses. This is because the convection and conduction heat flow along the welding direction, which are ignored in a 2D analysis, can be approximated by properly distributing heat flux into the two ellipsoids.

A.3 THERMAL STRESS ANALYSIS

Upon completion of the heat transfer analysis, the temperature history can be used as input to a thermal stress analysis. Classically the mechanical response of a weldment during and after welding can be represented by a series of differential equations, developed from equilibrium and compatibility conditions. These equations are inter-related through the constitutive model. In theory the solution to these equations can be obtained if the material properties, constitutive model and the boundary constraints are defined. The material properties required for the analysis are the Elastic Modulus, Poisson's ratio, yield stress, strain hardening and creep characteristics, thermal expansion coefficient and volumetric dilatation due to allotropic phase changes; all temperature dependent quantities. In addition yield stress and dilatation can be cooling rate dependent. One must anticipate that the modelling of the thermal stress behaviour will be highly complex with a non-linear solution. Furthermore if creep effects are included in the model the problem becomes time dependent.

Because of the complex non-linear material behaviour occurring during welding, it is impossible to predict residual stresses using analytical techniques without making a number of simplifying assumptions. The most common assumption adopted by researchers in analytical studies is that during welding only longitudinal stresses parallel to the weld

develop while all other stresses are zero. This simplifies the problem to a spatially one-dimensional analysis.

Early studies such as that by Gruning [A.33] considered only elastic behaviour with residual stresses being assumed equal to the thermal stress at the instant of welding. Boulton and Lance-Martin [A.34] were the first to account for plasticity in the analysis by defining a plastic zone in the vicinity of the weld bead and an elastic region elsewhere. Rogers and Fletcher [A.35] predicted residual stresses of a thermit weld at the centre line of a flat plate using a step by step graphical integration approach with perfectly plastic and temperature dependent material properties. The incremental thermal stress of each step was calculated using updated material properties, which were then added to the stress distribution from the previous step of the analysis. The final stress state at the end of each step was arrived at by superimposing an uniform equilibrium stress to create a plane strain condition in a section normal to the weld. Tall [A.36] developed a similar one-dimensional model except that the analysis was directed toward an actual moving weld heat source instead of a thermit weld. In addition the equilibrium condition was established implicitly in the solution procedure through iteration within each step of the analysis. At M.I.T., Masubuchi et.al. [A.37] developed a computer code for calculating thermal stress for bead on plate welds using Tall's model. Bryan [A.38] improved the program so that it could handle any type of strain hardening material behaviour and Nishida [A.16] further modified it to include multi-pass welds analysis.

Papazoglou [A.11] used the program developed at M.I.T to study bead-on-plates welds, and found that the one-dimensional program

produced results in good agreement with experiments for thin plates of up to 1/2 inch thickness. This good agreement in thin plates is because all stresses except longitudinal stress are very small. However for welding of heavier plates such as in multi-pass weldments, one-dimensional analysis is of limited accuracy because the stresses in the transverse and thickness direction become significant as compared to the longitudinal stress. In such cases a two or three dimensional analysis using numerical techniques is more appropriate.

The most appropriate numerical method to model the non-linear mechanical response of a weldment occurring during welding is the finite element technique. Hibbitt and Marcal [A.1,A.21,A.39,A.40,A.41] were the pioneers in applying the finite element method to predict weld residual stresses. Similar approaches to the welding stress problem have been adopted by other researchers, notably Masubuchi [A.6,A.14,A.15,A.37,A.42-A.46] at M.I.T., Rybicki [A.47-A.54] at Battelle Memorial Institute and Ueda [A.55-A.59] at Osaka University, Japan. A review of their work follows, with an emphasis on the modelling techniques adopted rather than the finite element method itself.

A.3.1 Dimensionality of the Problem

The stress distribution in a weldment is three dimensional in nature. However, as with the heat transfer analysis, a three-dimensional model is too costly to be practical. Except for very complex geometries, researchers have simplified the problem to the two-dimensional analysis of a plane normal to the weld far from the ends of the weld. Typically Friedman [A.26] studied GTA welding of flat plates using a 2D model with a plane strain assumption at the plane of analysis. He conceded however that such an assumption is only adequate in regions removed from the

weld pool. Hibbitt [A.21] modeled a GMA circular weld on a disc using an axisymmetric model with a section along the radial direction and perpendicular to the weld. Papazoglou [A.11] studied residual stresses in GMA butt welded plates using a 2D model and a plane strain assumption. However he pointed out that if bending distortion occurs in the welding direction, then a generalized plane strain condition, which can simulate the bending effect without utilizing a 3D model, should be adopted. Bending distortion in the welding direction occurs if the weldment is not properly restrained during welding.

A.3.2 Constitutive Model

Residual stress prediction requires an accurate material constitutive model for the entire temperature range during welding. Most researchers adopt a time independent plasticity model with temperature dependent material properties. One assumption of the time independent theory is that time dependent creep effects occurring at high temperatures are negligible. The basic argument as put forward by Hibbitt [A.39] is that the time spent at high temperatures during welding is so short that only primary creep can occur and the yield strength at high temperature is so low that primary creep has little effect on the final stresses. Hibbitt demonstrated this by including creep effects in his analysis using a simplified technique and finding that it made very little difference to the residual stress results.

The Von Mises yield criterion and an associated flow rule by Prantl-Reuss are commonly adopted in welding analysis to model the time independent plastic material behaviour [A.60]. The Von Mises yield condition is believed to be most suitable for metals in which yielding

is generally not affected by hydrostatic stress. A flow stress must be given to define the beginning of yield and this material data is usually assumed temperature dependent. However, Hibbitt [A.39] suggests that the dependence of flow stress on cooling rate is also important. The wide range of cooling rates in a weld can produce a significant range of alloy phases due to segregation as well as grain size, and this would have a strong effect on the low temperature flow stresses. Buyukozturk and Hibbitt [A.40] have included this effect in a study of multi-pass welds by simply dividing the weldment into zones of different cooling rates and assuming flow stress as a function of temperature appropriate to that zone. However no comparisons were made to determine the significance of the cooling rate dependence of flow stress on the final stress distribution.

Assuming a Von Mises yield criterion in the plasticity model, researchers [A.21,A.40,A.41] often found triaxial tensile stresses in the weld fillet region as high as three times the uniaxial yield stress. These high tensile triaxial stresses in conjunction with the presence of secondary phase in the fillet region suggest that void nucleation and growth can easily occur, and indeed this has been observed experimentally by Corrigan [A.61]. However the Von Mises yield criterion is not capable of modelling this void nucleation and growth. Gurson [A.62] has developed a simple model to include this effect on a macroscopic scale by introducing a hydrostatic yield dependence and an associated flow rule, so that the shape of the yield function changes as the void ratio increases. Hibbitt [A.39] suggested that this model can be used in a weld residual stress analysis provided that a void nucleation criterion can be established.

A strain hardening assumption is also required to describe the plastic deformation after yielding has begun. Both isotropic [A.17] and kinematic [A.11] material hardening have been assumed in various welding analyses. However, kinematic hardening is preferred, because the Baushinger effect due to reverse yielding is expected in welding and is more easily modelled by the kinematic hardening assumption [A.11,A.63] .

A.3.3 Material Properties

In a welding stress analysis the constitutive model must be defined over the full temperature range and include the effect of any metallurgical change. That is the elastic behaviour, plastic behaviour, average coefficient of thermal expansion and volumetric dilatation must be known as a function of temperature. If time dependent creep effects are also to be included in the model, then creep behaviour must be known.

The material behaviour in the elastic range can be described by the Elastic Modulus (E) and Poisson's ratio (ν), while in the plastic range it can be described by the flow stress (σ_y) and the strain hardening Modulus (E_s). Data for the above mechanical properties are seldom available for the high temperatures associated with welding. Thus extrapolation of existing data to higher temperatures is often necessary.

For the Elastic Modulus and flow stress, the extrapolations are straight forward as both properties reach zero value at the melting temperature. However Papazoglou [A.11] pointed out that arbitrary small values for E and σ_y should be used above the liquidus instead of zero to avoid numerical instabilities in the finite element model.

Hibbitt [A.21] adopted a linear increase in Poisson's Ratio from room temperature to the melting temperature. A Poisson's Ratio of 0.454 was assumed above the liquidus so that the molten metal approaches an

incompressible condition. Friedman [A.26] in a welding analysis of Inconel Alloy 600 reported that a better choice for the temperature variation of Poisson's Ratio would be one which provides a constant Bulk Modulus B , in conjunction with the Elastic Modulus, where $B = E/3(1-2\nu)$. This assumption leads to a maximum Poisson's Ratio of 0.5 above the liquidus, or an incompressible state in the molten pool.

The Strain Hardening Modulus (E_s), which describes the stress/strain ratio in the post-yield region, is usually a function of temperature as well as the accumulated plastic strain (ϵ_p). In a study of narrow gap arc welding, Ueda [A.55] approximated the non-linear post-yield behaviour of a SM-50 steel at a given temperature by two strain hardening moduli, one for ϵ_p less than 4% represented by $E_s = (\sigma_u - \sigma_y)/0.04$, where σ_u is the ultimate strength, and one for ϵ_p equal to or above 4% represented by $E_s = 0.0$. Papazoglou [A.11] further simplified this by assuming a single Tangent Modulus (E_T) to describe the post yield behaviour. At higher temperatures where stress-strain curves were not available, he assumed that the ratio of E_T/E remains a constant at all temperatures so that E_T at any temperature θ can be calculated as,

$$E_T(\theta) = (E_T/E)_{\text{room}} E(\theta) \quad (\text{A.3})$$

Also required for the stress analysis is the average thermal expansion coefficient, α , which is a temperature dependent quantity. This value is seldom available at high temperatures, thus extrapolation of existing low temperature data to the melting temperature is necessary. In addition, volumetric dilatation is likely to occur for material that undergo allotropic phase transformations. Most researchers model this phenomenon using a combined dilatation and thermal expansion curve

obtained under equilibrium transformation conditions. During welding, however, non-equilibrium transformation take place and the transformation temperature becomes a function of the cooling rate. Macherauch [A.64] indicated that the final residual stresses are greatly affected by the transformation temperature at which the volumetric dilatation occurs because of the wide range of material stiffness during welding. Papazoglou [A.11,A.46] attempted to simulate this non-equilibrium transformation effect in a more accurate manner. First he predicted the transformation temperature and the degree of transformation at a material point using CCT curves and cooling rates obtained from the heat transfer analysis. An appropriate amount of volumetric dilatation proportional to the degree of transformation was then added to the thermal expansion data at the transformation temperatures. This resulted in different expansion curves for the heating stage and cooling stage for each material point.

A.3.4 Plastic Strain Relieving

Material in the vicinity of the weld is loaded to a plastic state during welding, resulting in the accumulation of plastic strain. However, if the material is heated above the melting temperature, the accumulated plastic strain will be completely relieved. Papazoglou [A.11] simulated this material behaviour in a study of GMA welds by removing all accumulated plastic strain from the stress analysis at locations where the temperature was above the liquidus. Friedman [A.26] used a similar technique to model complete strain relief above the liquidus in the simulation of a GTA weld.

At temperatures below the melting point, plastic strains can be reduced by an annealing process whose effects are significant above the recrystallization temperature. Friedman [A.26] simulated this partial relief of plastic strains at high temperatures by a reduction factor $f(T)$ given by,

$$f(T) = e^{\beta(T-T_m)} \quad \text{for } T \leq T_m \quad (\text{A.4})$$

where T and T_m are the temperature at the material point and the melting temperature respectively, and β is a parameter proportional to the activation energy for the annealing process.

A.4 MULTI-PASS WELDS

The majority of welds are made of a number of weld passes. Commonly fewer than 10 passes are used however many hundreds can be used in thick section, overlays and repair procedures. It is only recently that researchers have had sufficient confidence in the prediction of residual stresses in a single pass weld to attempt multi-pass welds. It must be recognized that modelling multi-pass welds is a considerable computational effort and many researchers have been forced to simplify their analyses by grouping or layering of weld passes. Little proof exists that such techniques are accurate or even approximate. For example Lobitz et.al. [A.65] conducted such an analysis for a girth welded cylindrical shell subjected to three GTA weld passes. Both the thermal and mechanical problems were analysed using the finite element method based on an axi-symmetric assumption. That is it was assumed that the weld metal was placed around the full pipe circumference simultaneously. The calculated transient strains were in good qualitative agreement with experimental data but only fair quantitative agreement.

Rybicki et.al. [A.50,A.51] have developed mathematical models for predicting transient temperatures and final residual stress distributions for a two pass welded pipe, which recognized individual passes and the change in joint geometry after each pass. The temperature histories during welding were simulated by an analytical model based on a moving point heat source in an infinite solid with additional heat sources included to eliminate heat transfer out of the pipe wall. The temperature distribution for each weld pass was calculated by applying the heat source at the centroid of that weld pass and assuming thermal properties independent of the temperature. The residual stress distribution was calculated using an axi-symmetric finite element program again assuming simultaneous application of the full circumferential weld. In order to reduce computation cost the exact temperature history was not simulated in the stress analysis. Instead it was assumed that the weldment undergoes only one heating phase and one cooling phase for each weld pass (see figure A.4). Using this method only the temperature distribution at a time before a weld pass is applied, one at some critical time during welding, and one before the next weld pass is laid, were required for the stress analysis. The critical time was selected to be when the weld material began to regain its stiffness. Linear interpolation of temperatures between these critical times were assumed. As a result any effect due to melting was not included in the analysis. The analysis results show good temperature predictions compared to temperatures measured experimentally. The authors suggest that the residual stress predictions are good qualitatively but not quantitatively compared to experiments. However their experimental data was limited to measurements made using 10 strain gauges and a trepanning technique.

The use of only one heating phase and one cooling phase per weld pass simplifies the stress analysis considerably. However, this technique quickly becomes expensive if a large number of weld passes are to be modelled. To overcome this problem, Rybicki and Stonesifer [A.52] developed models for a seven pass pipe weld (figure A.5) and a thirty pass pipe weld (figure A.6) using a layering technique. The same thermal model described for the two pass pipe weld was used to determine the temperature distribution for each weld pass. In the finite element stress analysis all weld passes in a row were grouped into a layer for the analysis procedure. The temperature distribution of each layer was represented by an envelope of the temperature distributions of all the weld passes within that layer (figure A.7). The calculated stresses in the 7 pass weld using this technique were in good qualitative agreement with experimental data. Grouping of layers of weld passes in the analysis procedure were also attempted. The thirty pass pipe weld was simulated by grouping thirty passes into three weld layers. However the result of this model did not agree with experimental data.

Scaramangas [A.66] at Cambridge University has reported comparisons between theoretical and experimental predictions of weld residual stress in large diameter thin walled butt welded pipe. His finite difference analysis modelled each weld pass but considered only the yield stress as being temperature dependent amongst the other material parameters. Plasticity was modelled for an elastic-perfectly plastic material using the method of successive elastic solutions developed by Mendelson [A.60]. Using an axisymmetric assumption Scaramangas attempted to predict through thickness variations of the various stresses due to welds of 6, 13 and 18 passes. His agreement with experimental results

from sectioning and hole drilling techniques appears very good. This is surprising considering the simplifications in the material model assumed and the axisymmetric assumption. One notes that his results appear to agree with the results of Rybicki et.al. [A.48-A.54] for thin walled pipe but do not agree for thick walled pipe containing more weld passes.

Ueda et.al. [A.55,A.56] have developed a similar grouping technique for the modelling of a twenty pass narrow gap arc weld of 100 mm thick plates. Since the welding process was narrow gap arc welding, each layer is formed by one weld pass only. In their analysis, an attempt was made to group two layers into one by considering only even number weld passes. The resulting residual stresses compared favourably with those obtained from an analysis which recognized all weld passes but with the computing cost reduced by one half. It was also found that the magnitude and location of the maximum tensile residual stresses was near the finishing bead, as predicted by conducting the analysis for the last few passes or just the last weld pass alone, agreed well with results from a complete simulation. This model was based on the assumption that in the region of the HAZ of the newest pass, the residual stresses produced by the previous weld passes are completely relieved. The work of Ueda and his coworkers is the most comprehensive in the field of multipass welds. His techniques for considering several weld passes are generally checked against the full multi-pass simulation. Unfortunately there does not appear to be experimental confirmation of his techniques.

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$P(x,y,z)$ = FIXED FRAME CARTESIAN
COORDINATES

$P(x,y,\xi)$ = MOVING FRAME CARTESIAN
COORDINATES

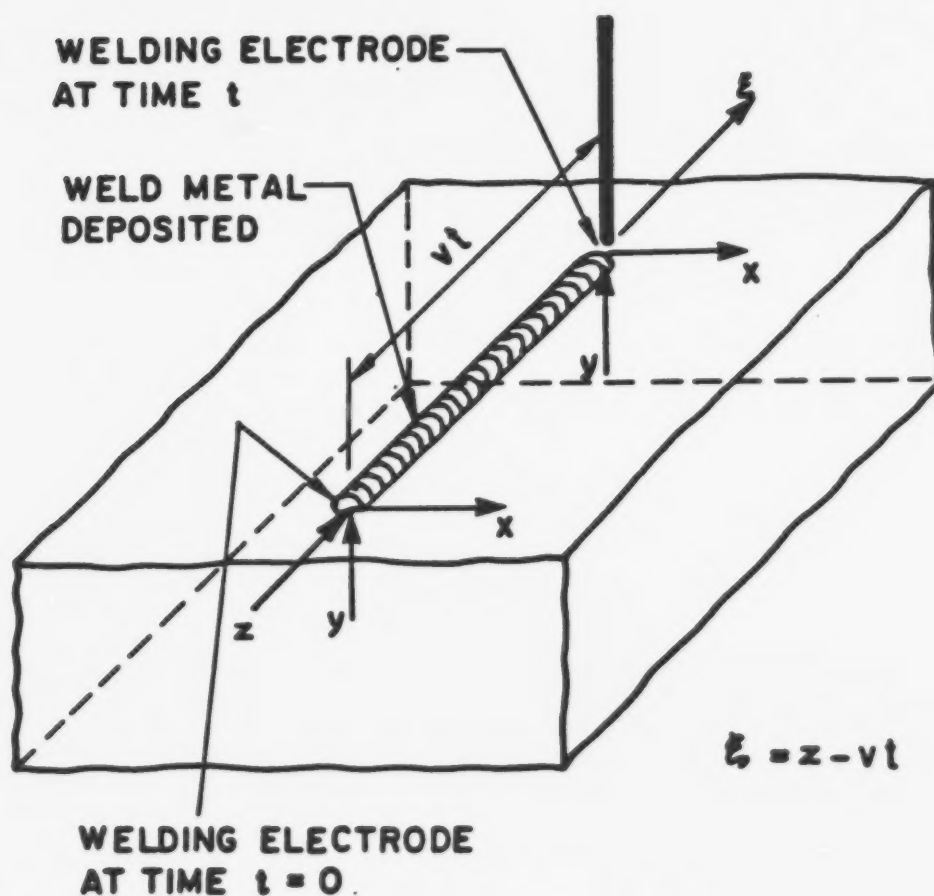


FIGURE A.1. WELDMENT CONFIGURATION

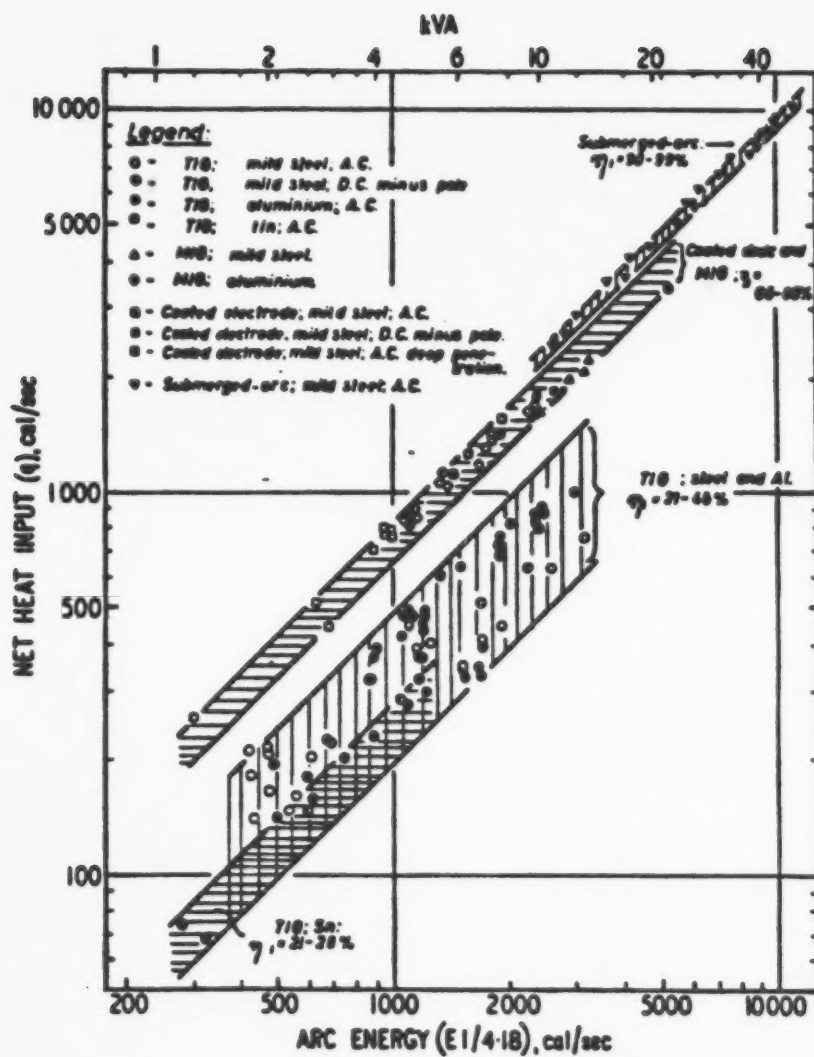


FIGURE A.2. ARC EFFICIENCIES OF DIFFERENT WELDING PROCESSES FROM [A.27]

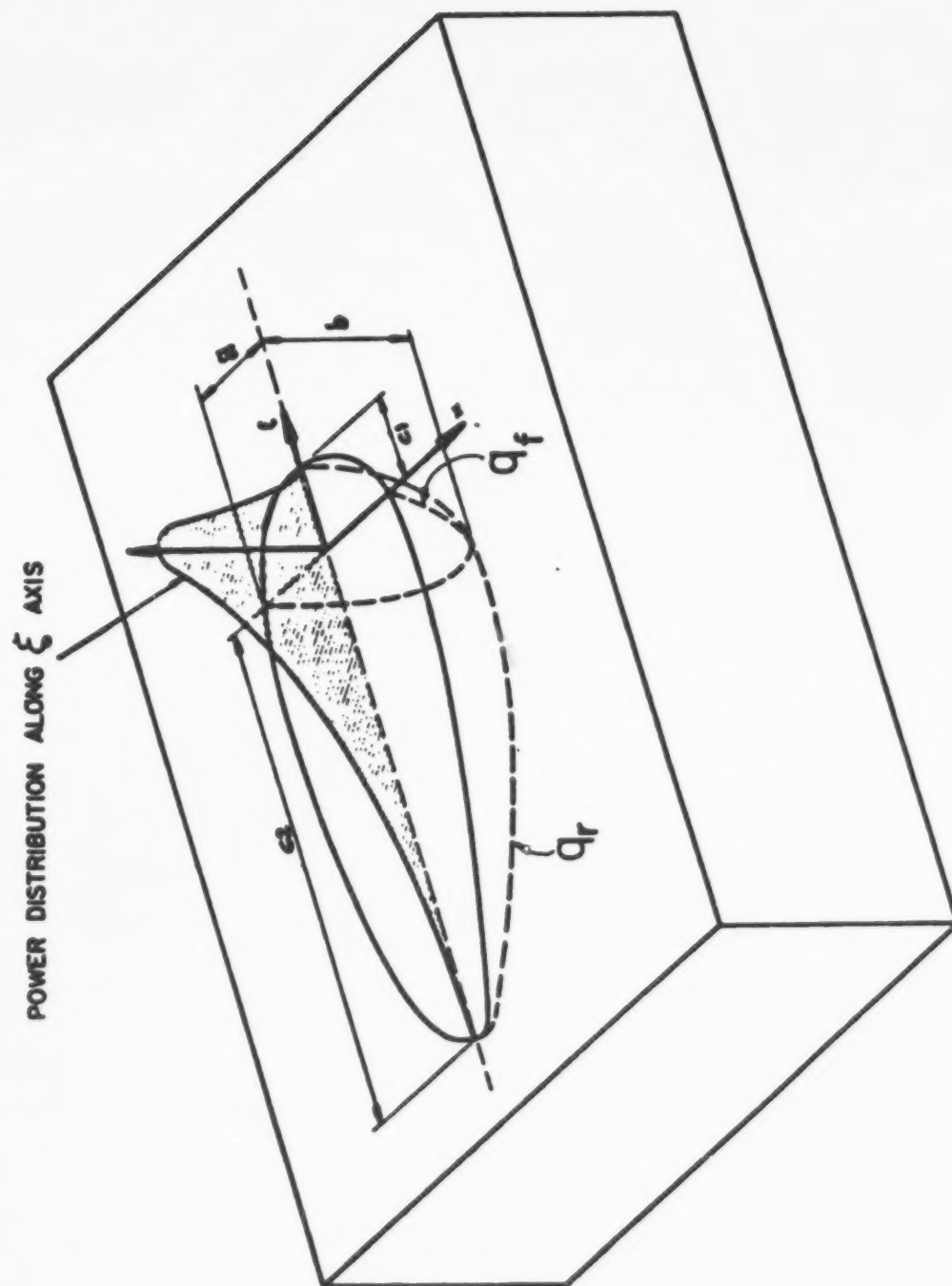
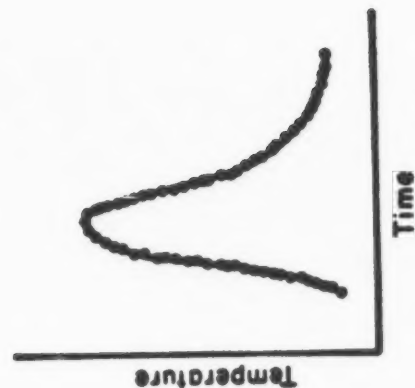
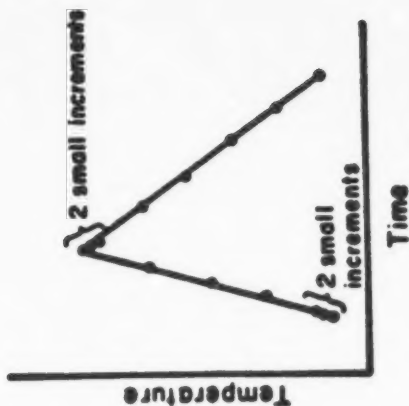


FIGURE A.3. DOUBLE ELLIPSOIDAL HEAT SOURCE MODEL FROM [A.32]

III Solution points



Rigorously follow the load history



Approximate the history with two piecewise linear segments

FIGURE A.4. TEMPERATURE TIME HISTORY MODELLING FOR AN ARBITRARY POINT DURING THE APPLICATION OF ONE WELD PASS

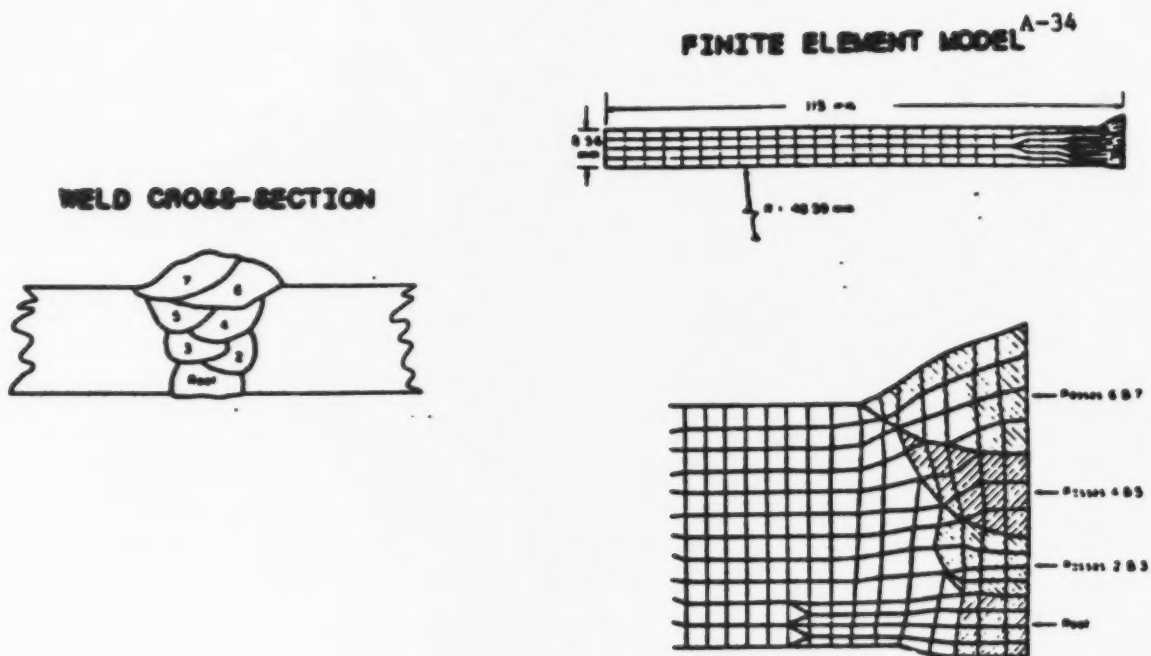


FIGURE A.5. SEVEN PASS GIRTH BUTT WELD

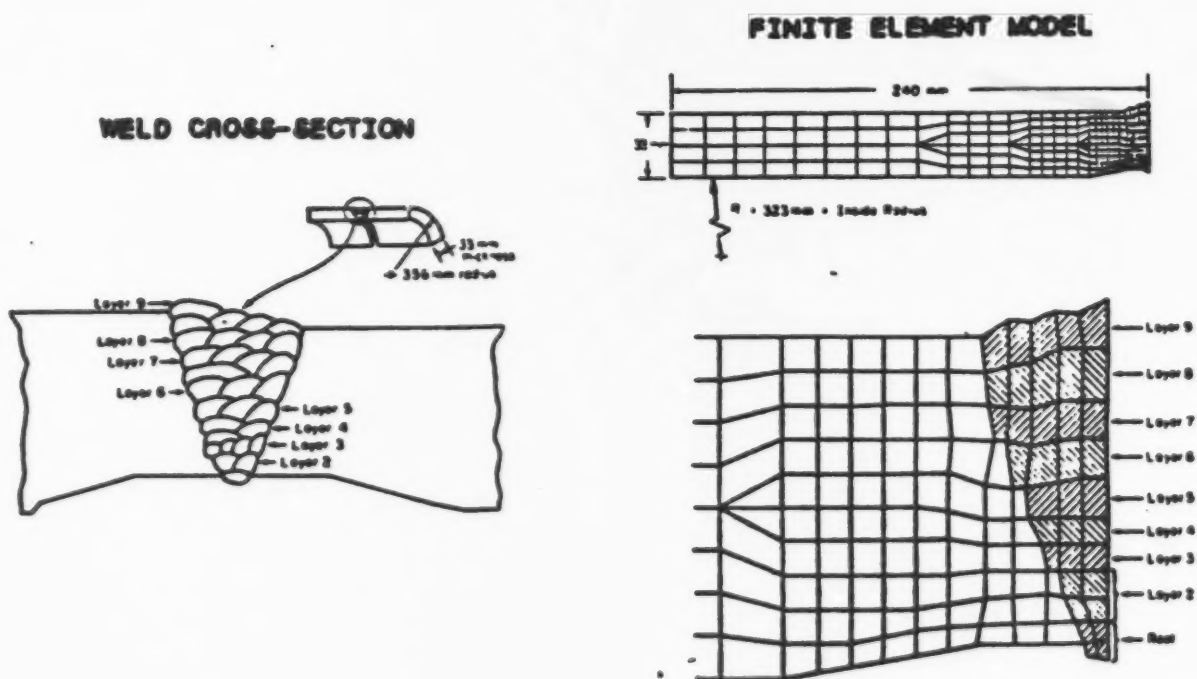


FIGURE A.6. THIRTY PASS GIRTH BUTT WELD

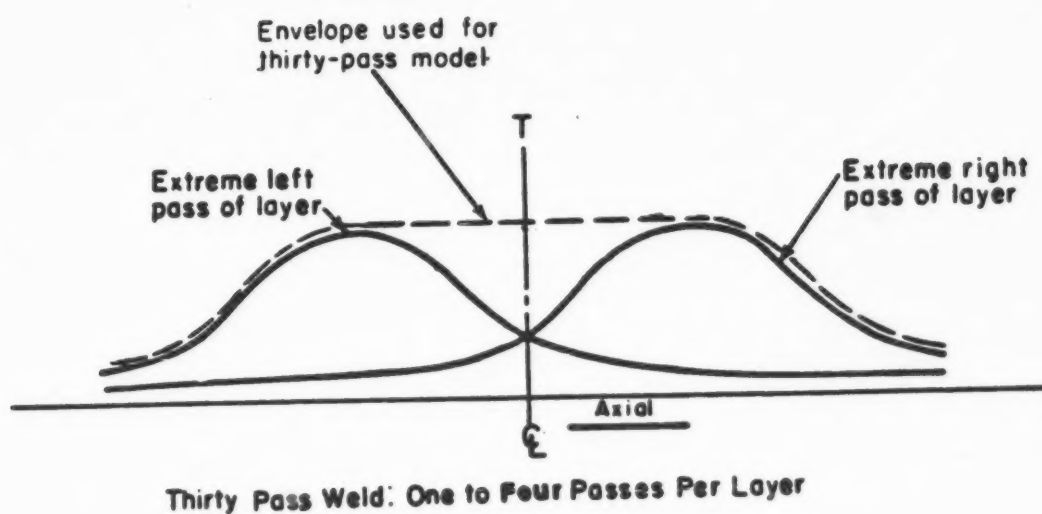
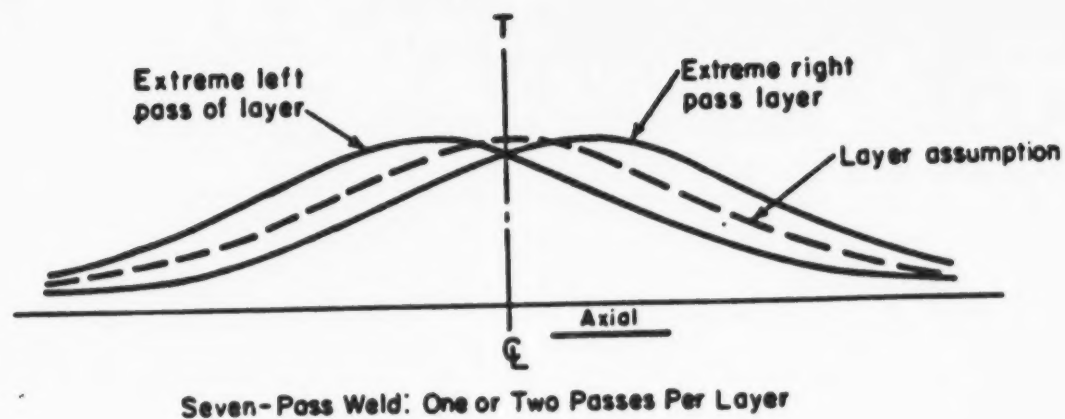


FIGURE A.7. METHOD OF TEMPERATURE DISTRIBUTION CALCULATION USING THE LAYER APPROACH

APPENDIX B

**TEMPERATURE AND DISTORTION MEASUREMENTS
IN SINGLE PASS WELDED STAINLESS STEEL PLATES**

Appendix B

TEMPERATURE AND DISTORTION MEASUREMENTS IN SINGLE PASS WELDED STAINLESS STEEL PLATES

B.1 INTRODUCTION

To determine a typical transient temperature distribution during welding and to provide specimens for the measurement of residual stress two welding tests was undertaken by the authors and Dr. M. Braid at the Department of Energy, Mines and Resources (EMR), Ottawa. This appendix describes this test and the results.

B.2 WELDING TEST

B.2.1 Specimen Geometry

Each test made use of a piece of AISI 316L stainless steel plate 610 mm x 610 mm x 13.2 mm with a typical composition shown in Table B.1. A groove as shown in figure B.1 was machined along the centreline of each plate for the deposition of weld metal. After machining, the plates were heated to 900°C, held for 4 hours and then slowly cooled to room temperature to remove any residual stresses.

B.2.2 Welding Procedure

Before welding, two tab plates 101 mm x 152 mm were tack welded to each plate for the start and stop of the weld (see figure B.1). To prevent lateral movement during welding the plate was secured onto a welding table by two C-clamps along one side. The plate was electrically grounded through the tab plate near the starting point of the weld.

The welding process was performed by an automatic submerged arc welding (SAW) machine in the welding section at EMR. The welding

conditions for the two test plates were identical and are given in table B.2. The welding wire used was type ER316L (SANVIK 2R62) for SAW welding. Its composition is listed in Table B.1.

During welding a hopper ahead of the weld electrode deposited a granulated flux blanket over the groove which concealed the arc and excluded air from the weld joint. The flux blanket was in place during all temperature readings. A typical cross section of the flux blanket is shown in figure B.2.

B.2.3 Instrumentation

B.2.3.1 Temperature Measurements

Temperatures on the top surface of the plate during welding were monitored by thirty K type Chromel-Alumel thermocouples mounted in three rows at the locations shown in figure B.3. The thermocouple wire size was 32 gauge (0.23 mm diameter) with an upper temperature limit of approximately 900°C. The measuring junction of the thermocouple was created by twisting the wires together and fusing them to form a bead by electric arcing in a mercury bath. The bead was attached to the plate by inserting it into a small hole drilled on the surface and then securing it with heat resistant ceramic cement (INSUL-TED) which can sustain temperatures up to 1000°C. The thermocouple leads were connected to an OSAR 8140 thermocouple scanner of an OPTILOG data acquisition system.

During welding the temperature history of each thermocouple was recorded by the OPTILOG in the form of digital voltages. Under the command of a host computer (an APPLE IIe), the OPTILOG scanned the set of thirty thermocouples at a speed of 33 channels/second as welding

began. After each set of readings, the recorded data was transferred back to the memory core of the host computer before another set of reading was taken. The time delay between each set of reading was approximately 3.6 seconds for the initial 30 sets of readings and was increased to 14.2 seconds thereafter. A total of 80 sets of readings were taken over a period of 13 minutes and 37 seconds.

After all readings were completed, the voltage readings stored in the core memory were converted into temperatures. The recorded digital voltages can be related to the bead temperatures by the following equation,

$$V_{out} = sk \times a \times (T - T_{junc}) + V_{os} \quad (B.1)$$

where V_{out} = digital voltage as recorded by OPTILOG

V_{os} = input offset voltage

T = temperature at the thermocouple bead

T_{junc} = temperature of the OSAR 8140 Scanner

sk = scaling factor (1000)

a = Seebeck coefficient

In the above equation, the input offset voltage measured was negligible and therefore can be assumed zero while the scanner temperature T_{junc} measured was very close to the room temperature so that it can be assumed to be T_{room} . The Seebeck coefficient of a K type thermocouple is almost a constant in the operating temperature range of 0 to 1000°C as indicated by the voltage versus temperature curve of figure B.4. As a result the Seebeck coefficient has been assumed to have a constant value of 42.0 microvolts/°C. Based on these

approximations the temperature at the thermocouple bead was estimated from the recorded digital voltage by the following equation,

$$T = \frac{V_{out}}{sk \times a} + T_{room} \quad (B.2)$$

B.2.3.2 Distortion Measurements

The weld metal is deposited into the groove while it is hot. Upon cooling the weld metal contracts and hence occupies a smaller volume. Because of the non-symmetrical distribution of weld metal about the mid-plane of the plate and the clamping of only one half of the plate, the free half of the plate rotates upward due to the weld contraction.

To measure this rotation a set of 6 dial gauges were mounted 25 mm from the free edge of the plate as shown in figure B.3 and figure B.5. Displacement readings were recorded after the plate had cooled to room temperature. Distortion in other directions are also possible but were not measured.

B.2.4 Discussion of Results

B.2.4.1 Temperature History

The temperature history of the thirty thermocouples during welding are presented in figures B.6 to B.8 for plate #1 and figures B.9 to B.11 for plate #2. Each figure shows the temperature history of a row of 10 thermocouples from time zero to 300 seconds. Time zero corresponds to the time when the welding electrode passes the row of thermocouples under consideration. Only the six thermocouples closest to the weld are shown as the other thermocouples showed temperature rises of less than 30°C.

A sample rate of 0.28 readings/second for the first 30 readings was sufficient to capture the time behaviour of the temperature except at the 10 mm thermocouple location where temperatures rose sharply to a peak and then were followed by an equally sharp decline. However the rate of 0.07 readings/second for the last 50 readings was more than sufficient during the slow cooling stage.

The temperature history plots for plate #1 show that during the cooling stage, two thermocouples (Row 1 @ 20 mm and Row 2 @ 15 mm) recorded temperatures lower than the adjacent thermocouples on the same row but further away from the weld. This is possibly due to poor thermal contact between the thermocouple beads and the plate material at these locations.

Small fluctuations of the recorded temperatures can be observed at 3 thermocouple locations on plate #2 (Row 1 @ 30 mm, Row 2 @ 40 mm and Row 3 @ 30 mm). This is thought to be due to the pick up of electrical noise from the plate. This problem has since been eliminated by the connection of a guard wire to the thermocouple wires.

Because of the above mentioned irregularities, the recorded temperature history from a single row of thermocouples can not be used directly for comparison with numerical analysis results. However since all figures are plotted from a similar zero time reference, the temperature history curve of thermocouples located at the same distance from the weld centreline can be superimposed. Selecting the most representative curve for each of the six locations (10, 15, 20, 30, 40, and 60 mm from the weld) results in the set of history curves shown in figure B.12. These curves were used for comparison with the finite element heat flow analysis results.

B.2.4.2 Distortion

The results of the distortion measurements are shown in Table B.3. The results of plate #1 show that there was an increase in distortion from the starting point to the end of the weld. The average displacement recorded at the six dial locations was 11.9 mm which can be translated to a rotation of 7 degrees upward (see figure B.13).

During the welding of plate #2, all dial indicators slipped on the surface as the plate rotated upwards, which resulted in substantially lower distortion readings (5.2 mm) than plate #1.

B.2.4.3 Weld Bead Shape

A photomicrograph of an etched cross-section of the single pass submerged arc weld from plate #1 is shown in figure B.14. The fusion zone has a semi-circular profile with a width of 12 mm and a depth of 6 mm.

Based on the fusion zone size and the weld groove dimensions, a 46% dilution of the parent metal in the fusion zone was estimated. A weld pool length of 4.0 cm was also estimated based on the ripple shape which appears on the surface of the weld.

TABLE B.1
TYPICAL COMPOSITION OF 316L STAINLESS STEEL PLATE AND ER316L
STAINLESS STEEL WELDING WIRE

ELEMENT	316L	ER316L	
		NOMINAL SANDVIK ANALYSIS	ACTUAL EMR ANALYSIS
C	0.030	0.016	0.017
Si	1.000	0.320	0.300
Mn	2.000	1.750	1.730
P	0.045	0.007	0.007
S	0.030	0.009	0.008
Cr	16.0-18.0	19.300	19.500
Ni	10.0-14.0	13.110	13.100
Mo	2.0-3.0	2.180	2.180
Ti	---	0.010	---
Co	---	0.050	0.049
V	---	0.022	0.030
Cu	---	0.050	0.034
N	---	0.038	---
Nb	---	0.010	0.140
Ta	---	0.010	---
Fe	balance	balance	balance

TABLE B.2
WELDING PARAMETERS

Voltage	34 Volts
Current	440 - 450 Amps
Travel Speed	88.9 cm/min
Stick Out	2.54 cm
Wire Feed	3.175 cm/sec

TABLE B.3
DISTORTION MEASUREMENT RESULTS (mm)

	DIAL GAUGE #						AVERAGE
	1	2	3	4	5	6	
plate #1	11.38	11.89	11.84	12.34	12.57	12.70	11.94
plate #2	5.28	5.64	4.24	5.28	5.16	4.95	5.21

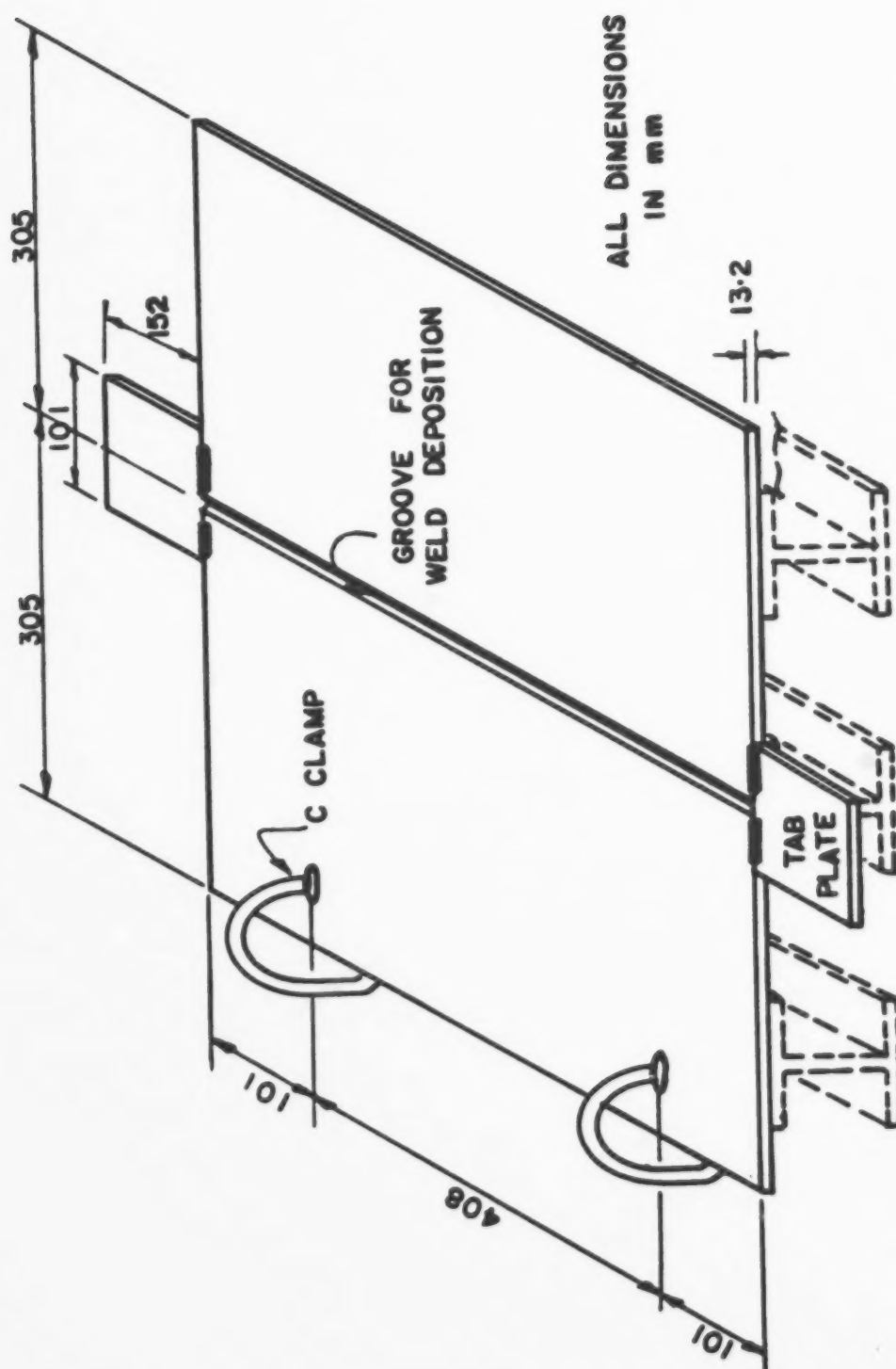


FIGURE B.1. WELDING TEST SET UP

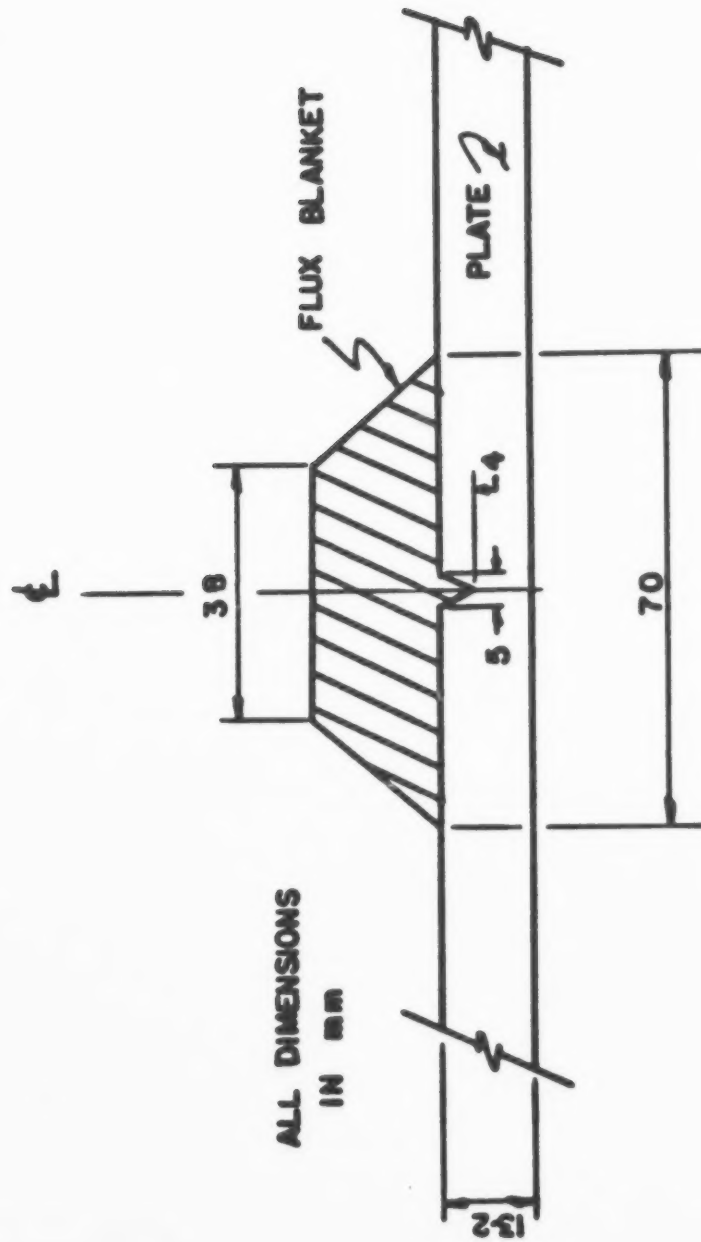


FIGURE B.2. FLUX BLANKET & WELD GROOVE GEOMETRY

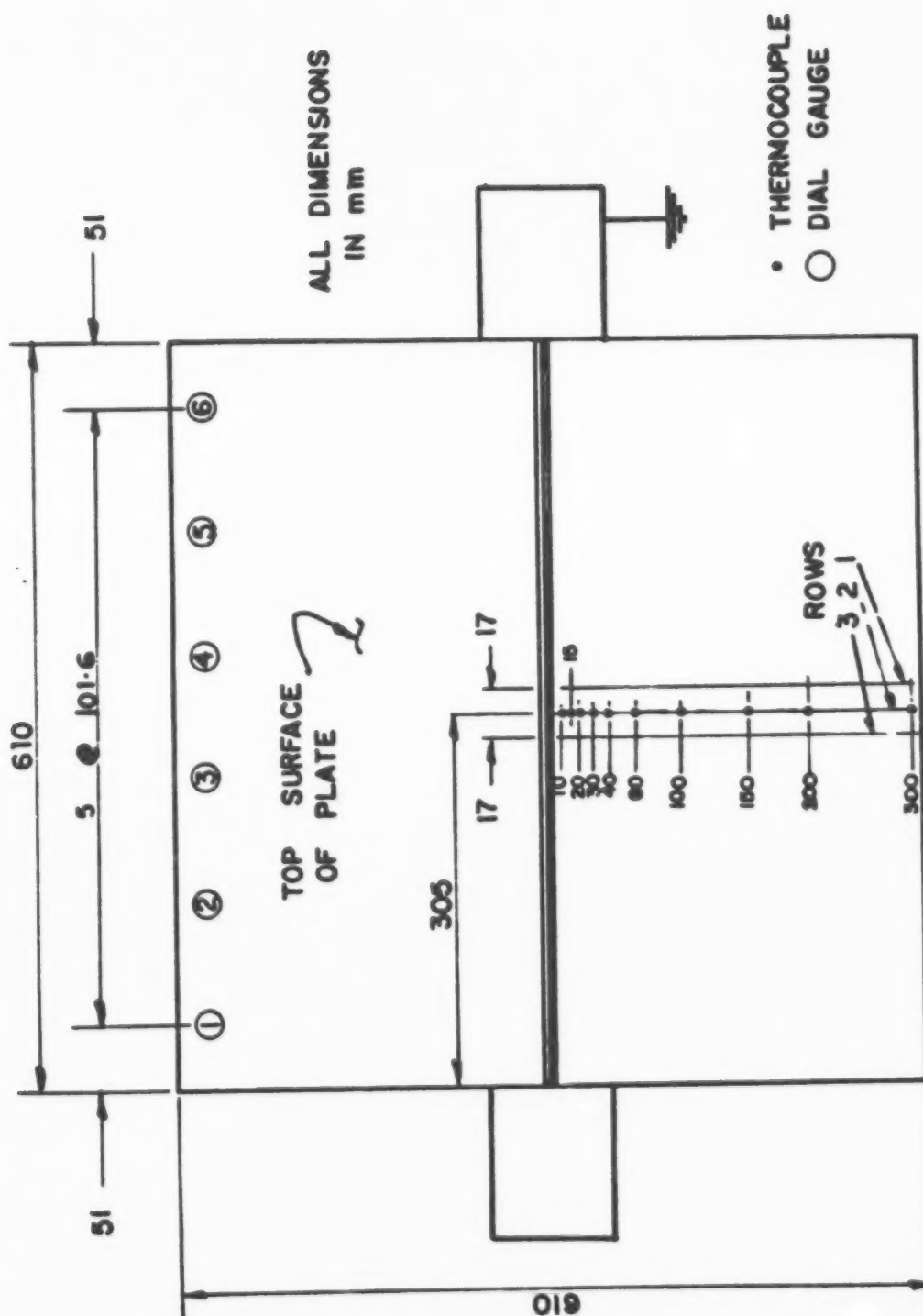


FIGURE B.3. THERMOCOUPLES & DIAL GAUGES LOCATIONS ON TEST PLATE SURFACE

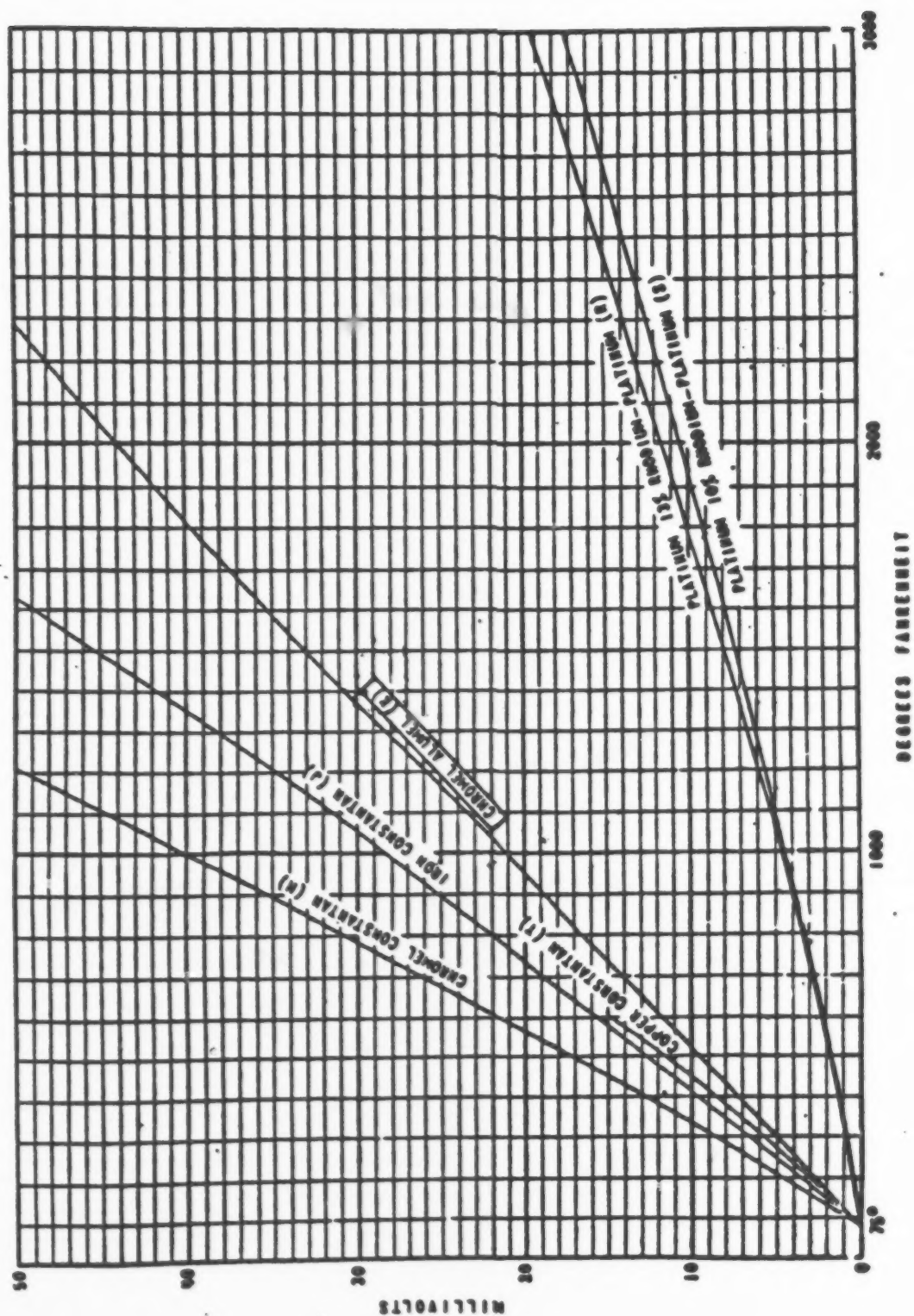


FIGURE B.4. TEMPERATURE-MILLIVOLT RELATIONSHIP OF K TYPE THERMOCOUPLES

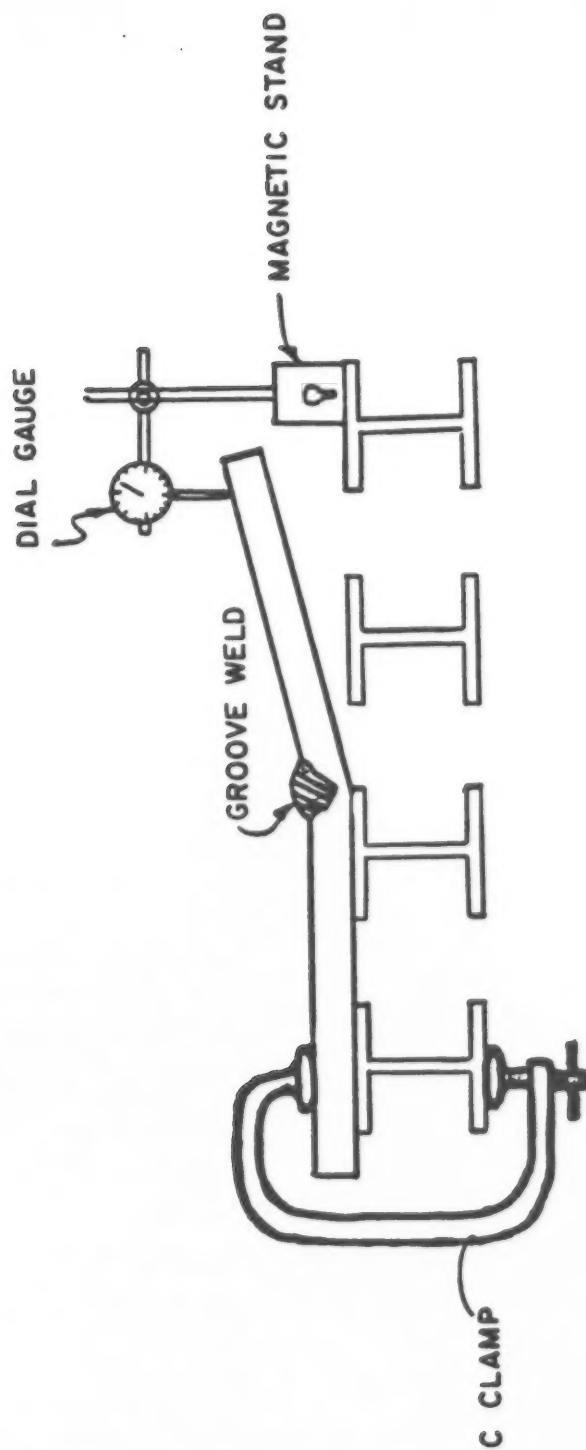


FIGURE B.5. DISTORTION OF 316I STEEL PLATE
AFTER WELDING

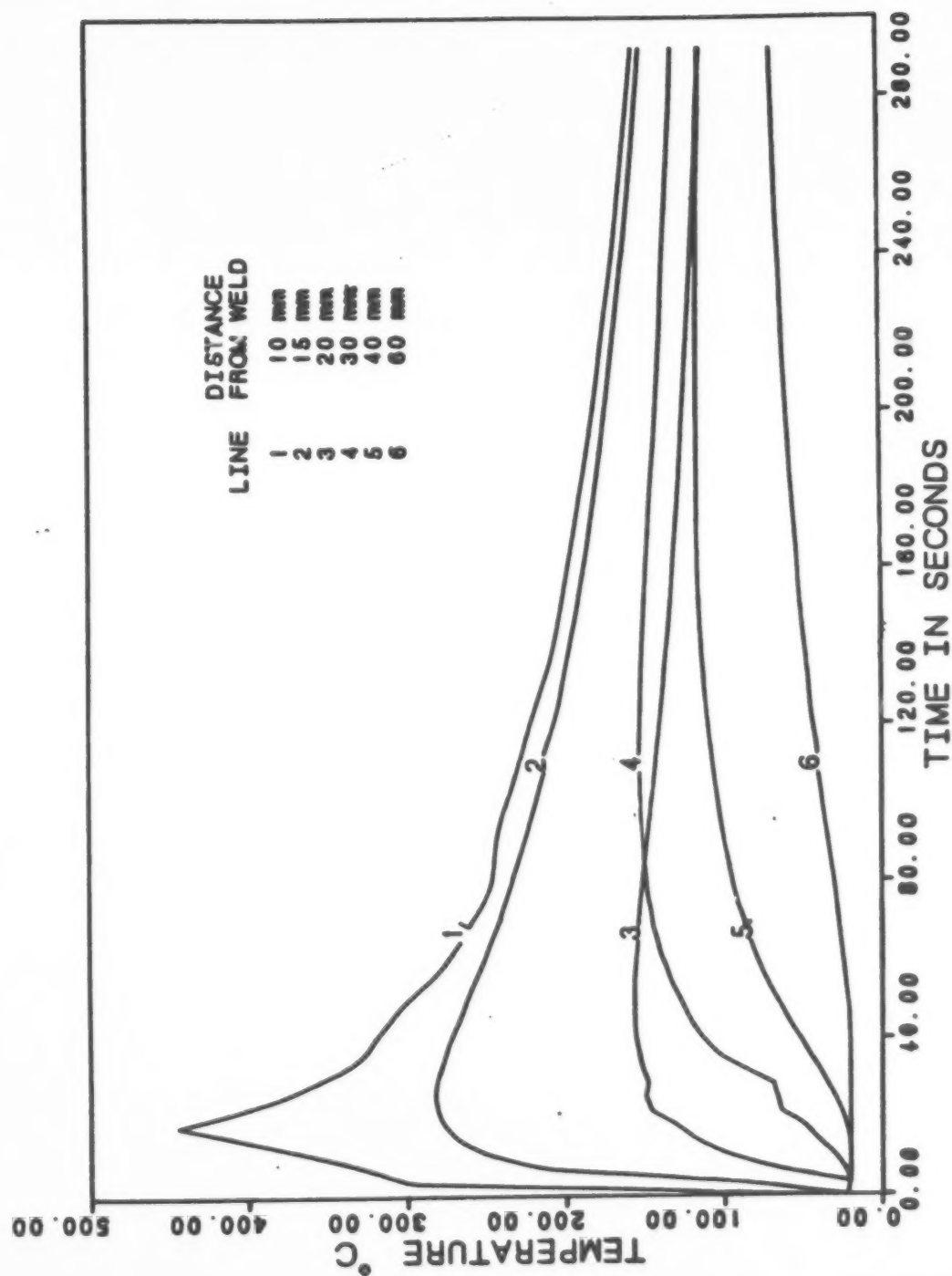


FIGURE B.6. TEMPERATURE HISTORY OF THERMOCOUPLES DURING SAW WELDING
ROW 1 OF TEST PLATE 1

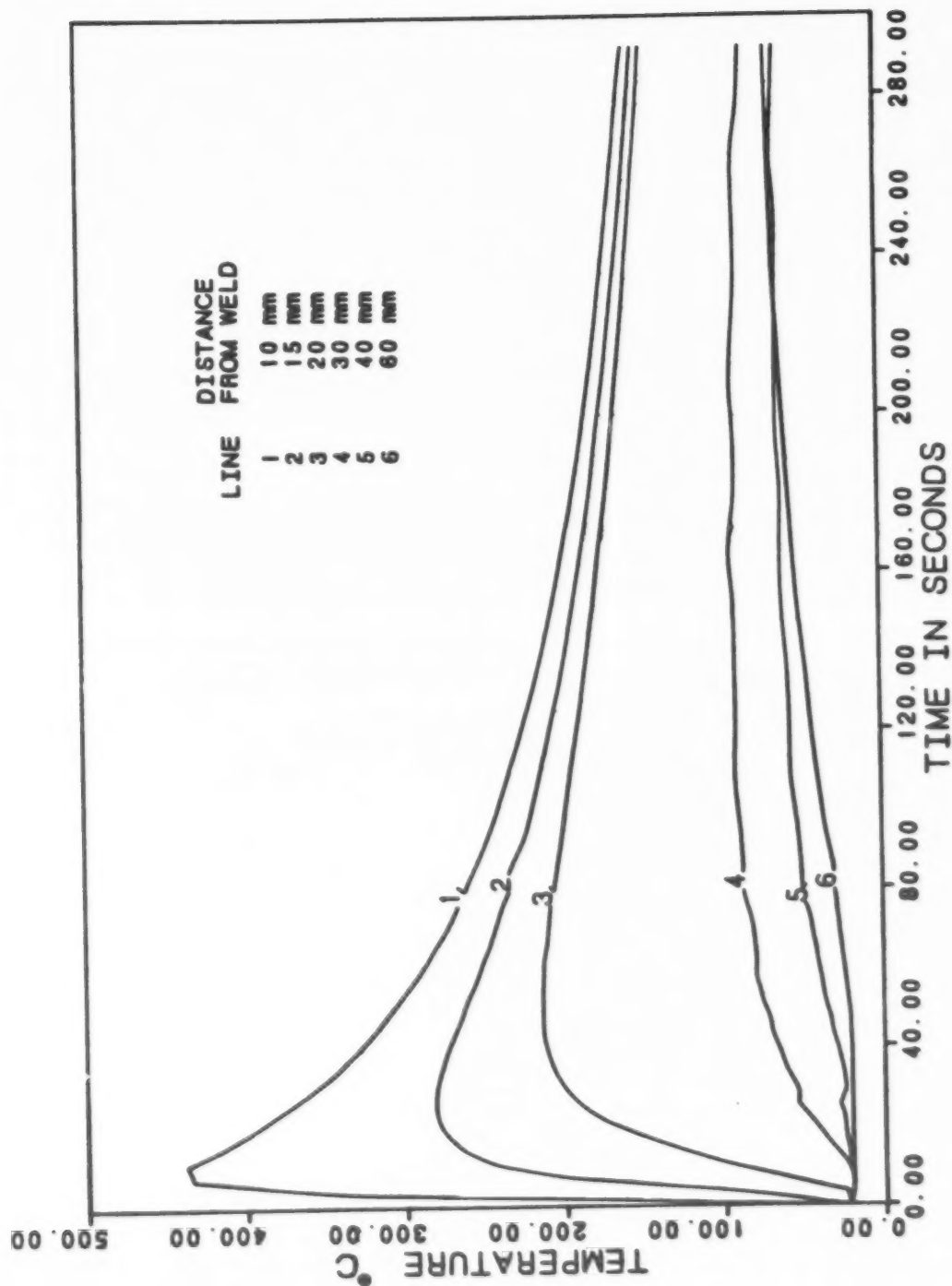


FIGURE B.7. TEMPERATURE HISTORY OF THERMOCOUPLES DURING SAW WELDING
ROW 2 OF TEST PLATE 1

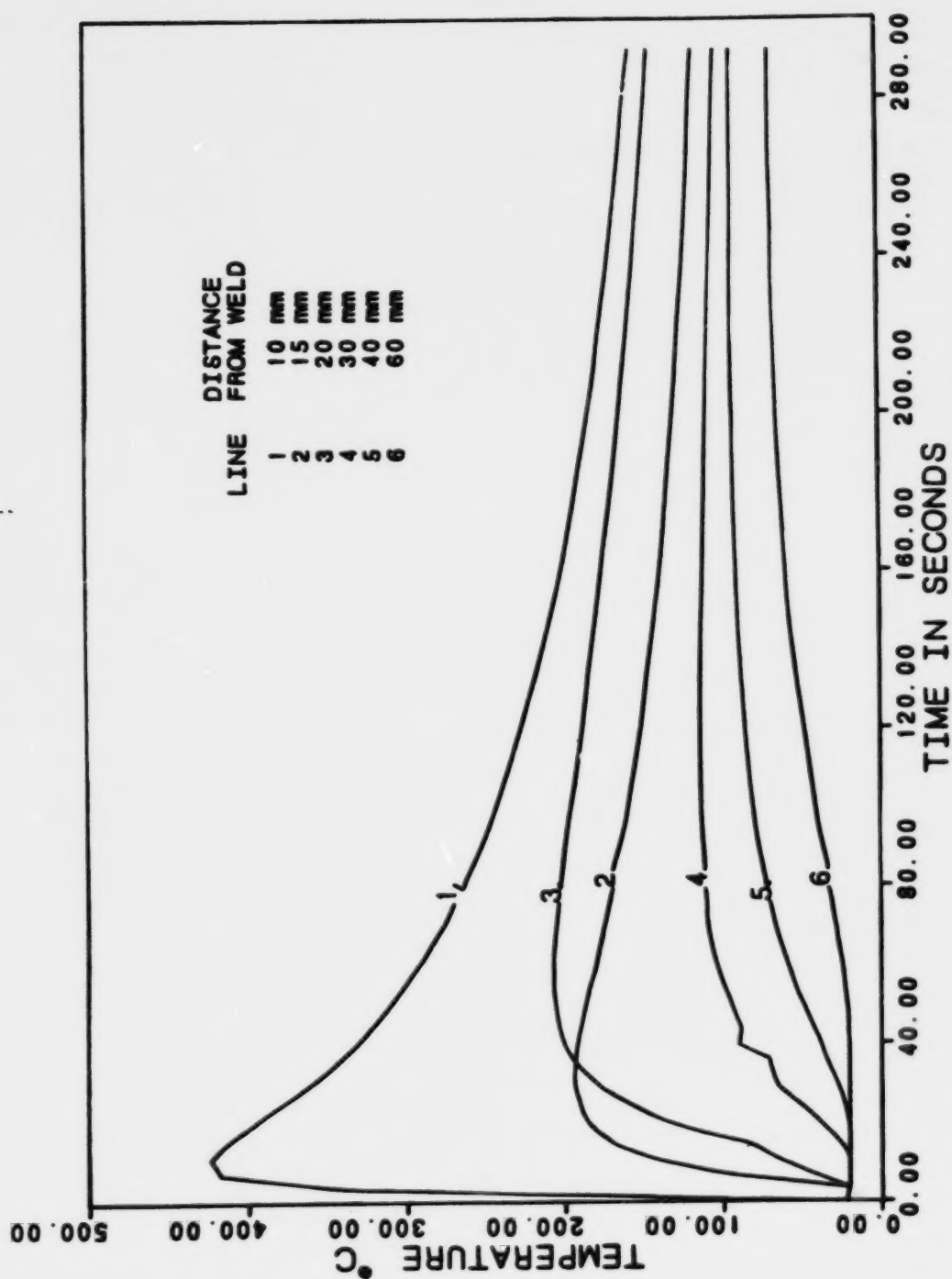


FIGURE B.8. TEMPERATURE HISTORY OF THERMOCOUPLES DURING SAW WELDING
ROW 3 OF TEST PLATE 1

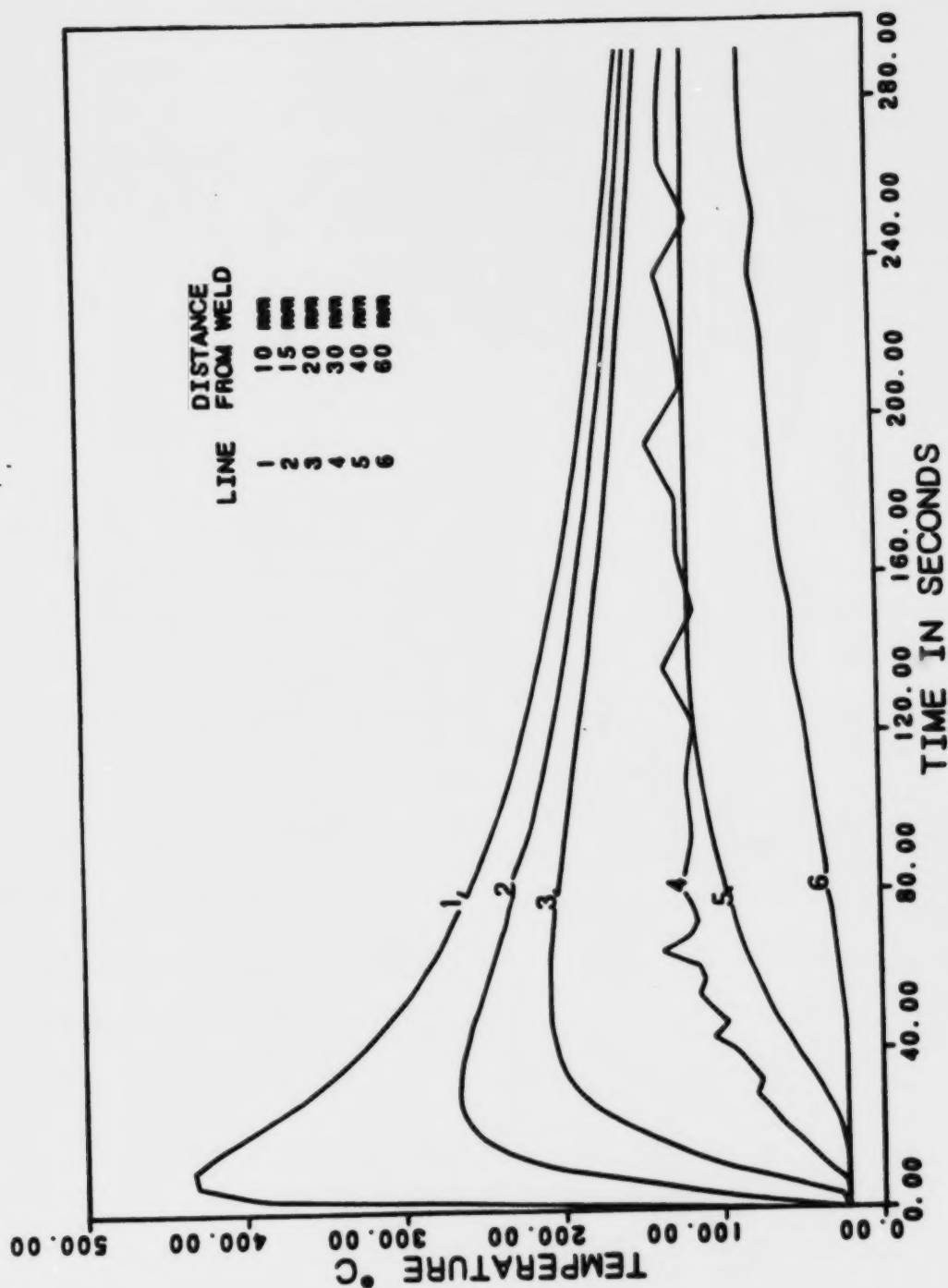


FIGURE B.9. TEMPERATURE HISTORY OF THERMOCOUPLES DURING SAW WELDING
ROW 1 OF TEST PLATE 2

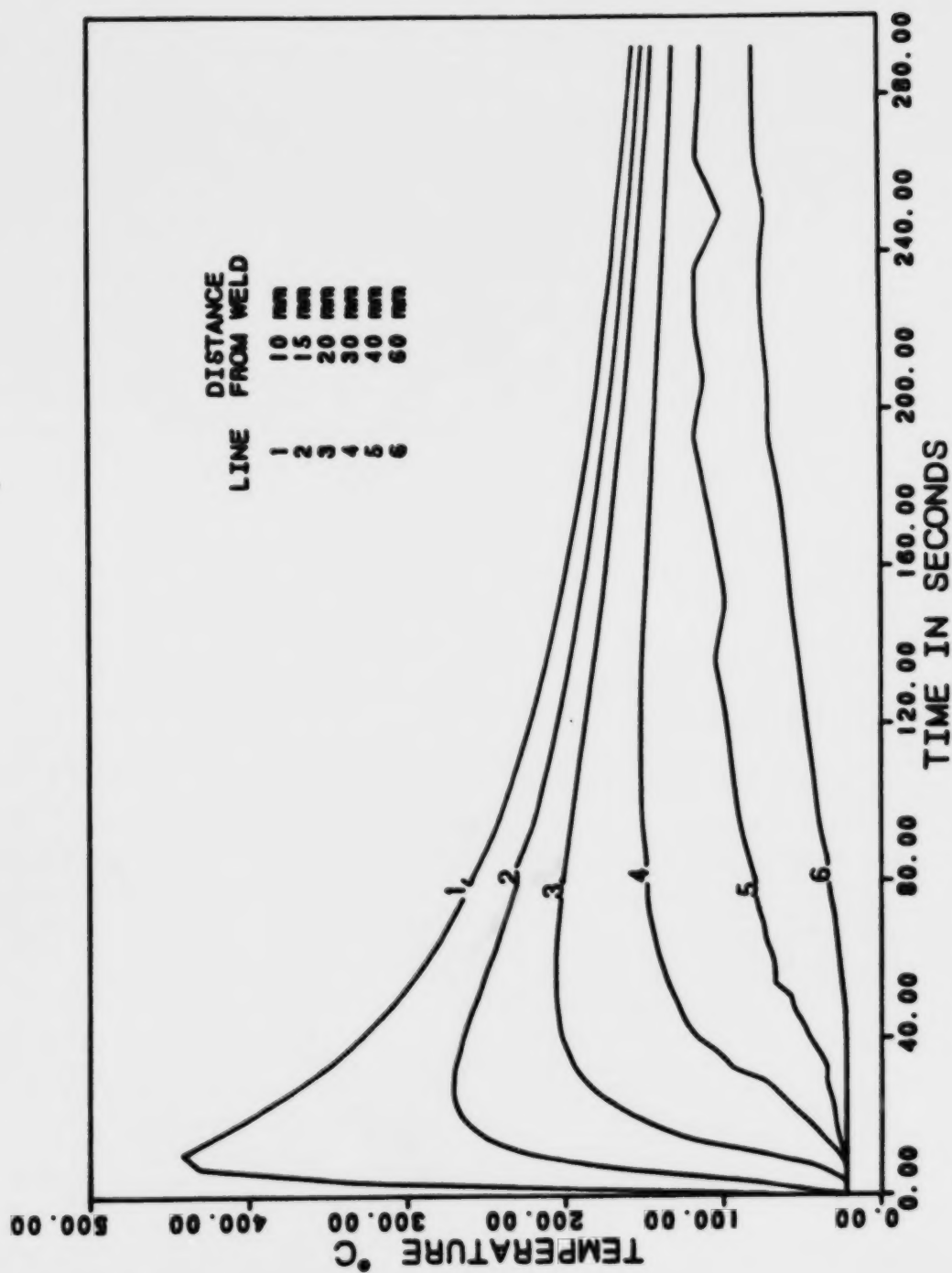


FIGURE B.10. TEMPERATURE HISTORY OF THERMOCOUPLES DURING SAW WELDING
ROW 2 OF TEST PLATE 2

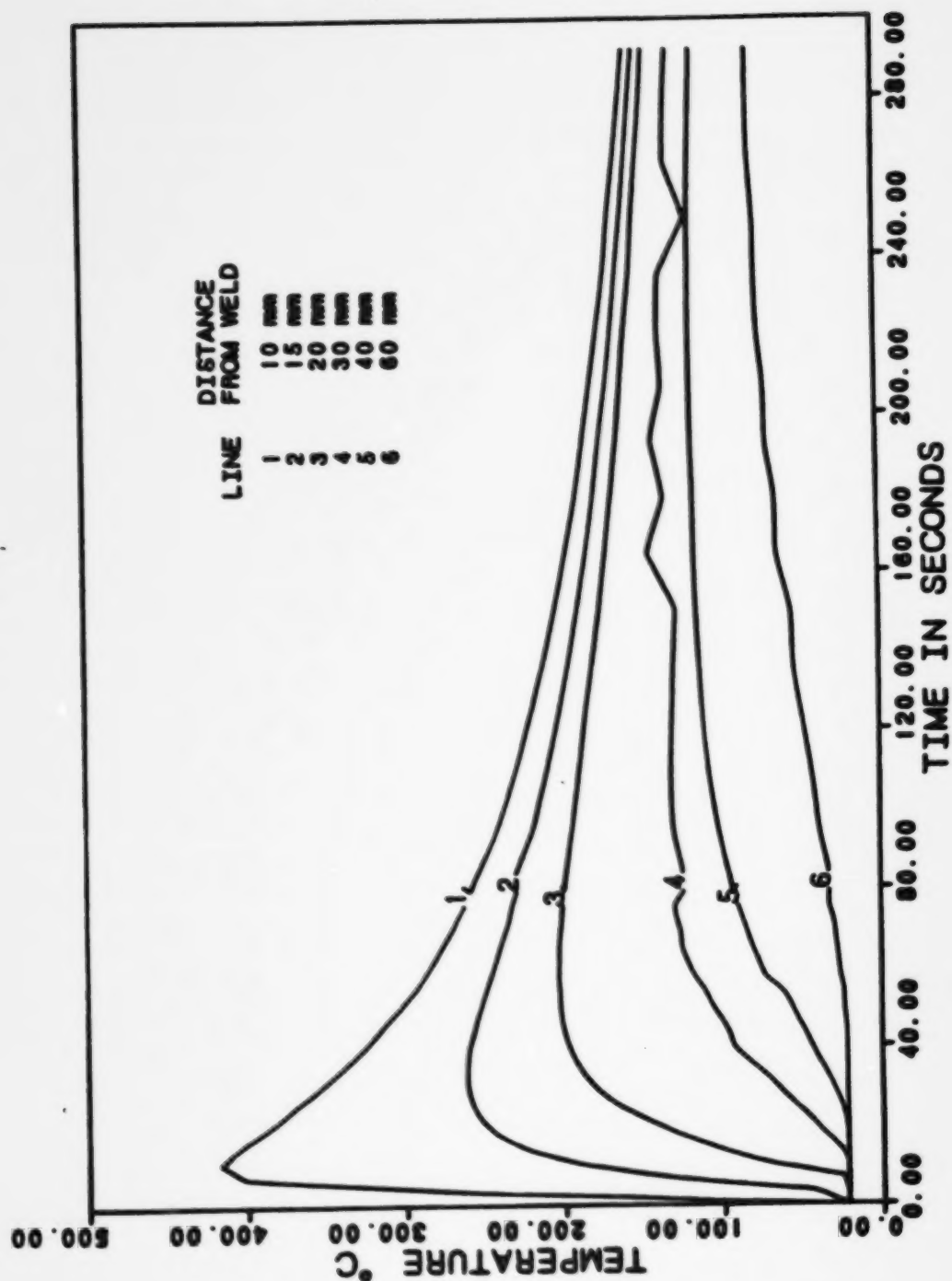


FIGURE B.II. TEMPERATURE HISTORY OF THERMOCOUPLES DURING SAW WELDING
ROW 3 OF TEST PLATE 2

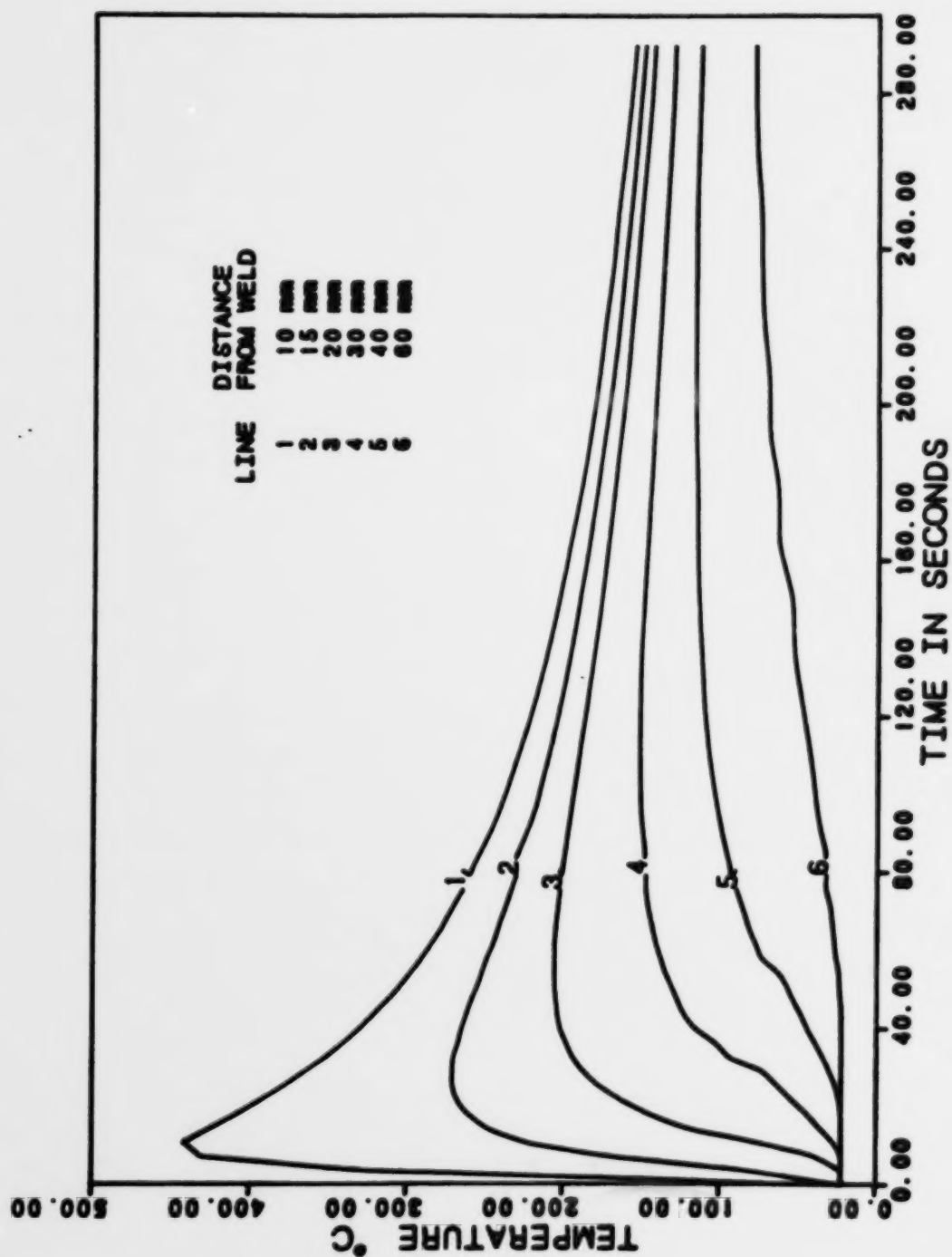


FIGURE B.12. TEMPERATURE HISTORY OF THERMOCOUPLES DURING SAW WELDING
COMBINED DATA FOR COMPARISON WITH NUMERICAL RESULTS

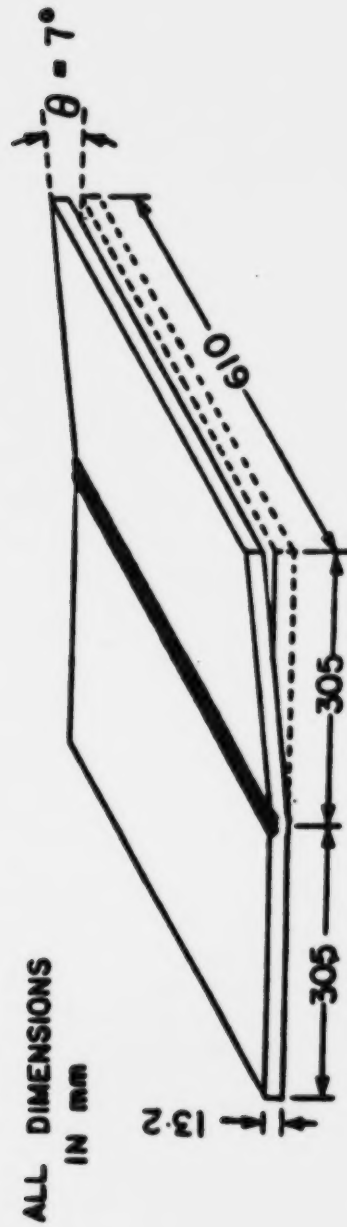


FIGURE B.13. DISTORTION OF TEST PLATE AFTER SAW WELDING

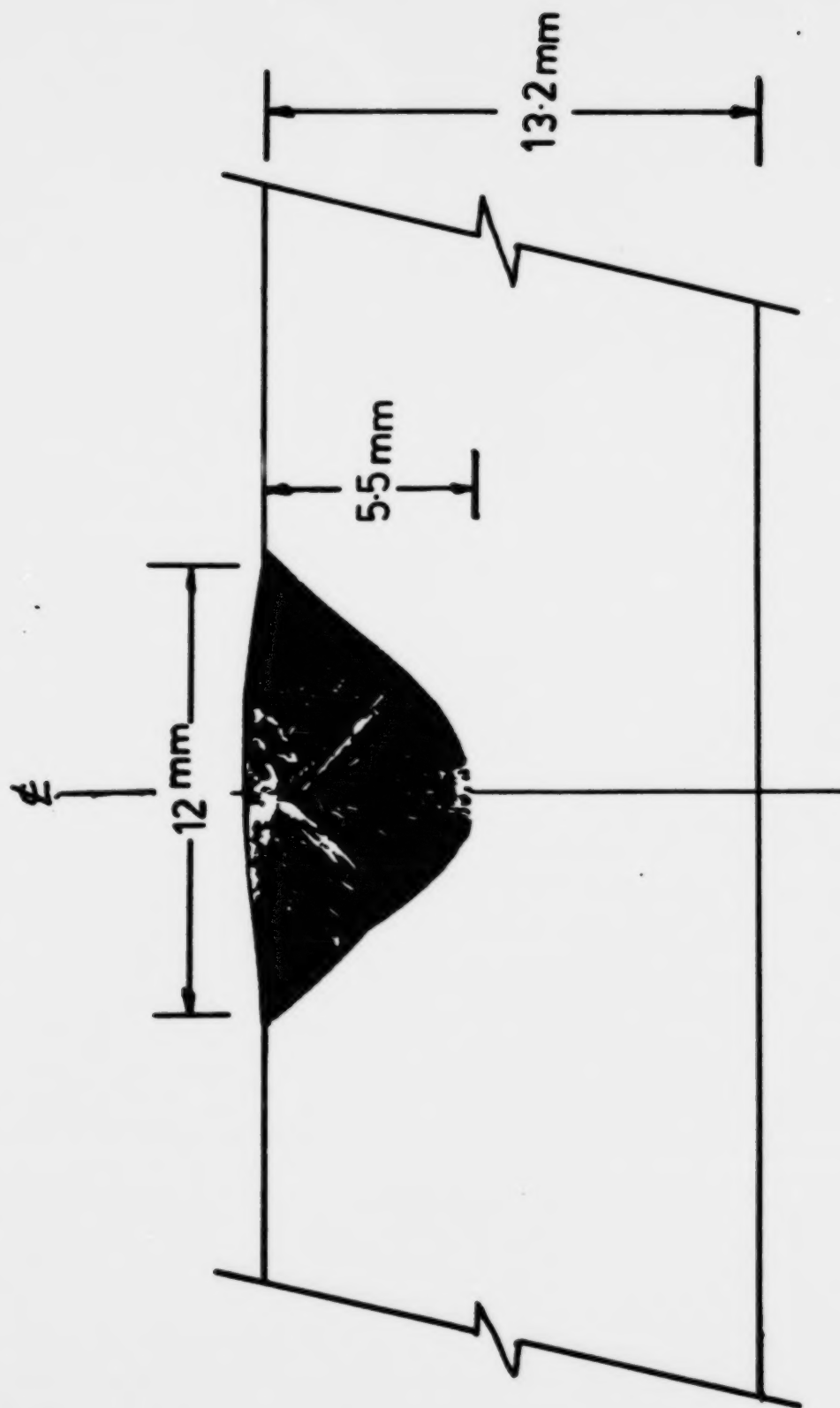


FIGURE B.14. A TYPICAL PHOTOMACROGRAPH OF AN ETCHED CROSS SECTION OF A SAW WELD

APPENDIX C

HEAT SOURCE MODELS

Appendix C

HEAT SOURCE MODELS

C.1 INTRODUCTION

A good heat source model is essential in obtaining an accurate temperature distribution in the weldment especially in the vicinity of the weld pool. One of the major limitations of analytical solutions to heat flow problems is that the heat source shape must be simple. The most complex analytical treatment of a heat source model reported to date is Papazoglou's [C.1] 3D skewed-normally distributed surface heat source. On the other hand the finite element method can handle heat sources of any shape easily. This permits the user to develop a more realistic model to simulate the actual weld pool behaviour. Many types of heat source models have been developed in the past for finite element heat flow analysis. Those most applicable to welding are described below.

C.2 CIRCULAR GAUSSIAN SURFACE FLUX MODEL

Pavelic et.al. [C.2] proposed that the heat source should be a Gaussian distribution of thermal flux deposited on the surface of the weldment (figure C.1).

$$q(r) = q(o) e^{-Cr^2} \quad (C.1)$$

where $q(r)$ = surface flux at radius r (J/m^2s)
 $q(o)$ = maximum flux at centre of heat source (J/m^2s)
 C = concentration coefficient
 r = radial distance from centre of heat source

The coefficient C is associated with the diameter of the hot spot d as,

$$d = 2\sqrt{C} \quad (C.2)$$

where d can be obtained experimentally.

In order to apply this to a 2D analysis along a plane perpendicular to the weld an alternative form of Pavelic's model, as suggested by Friedman [C.3], can be used where the flux, q, is expressed as,

$$q(x,t) = \frac{3Q}{\pi c^2} e^{-3 \left(\frac{x^2 + (v(r-t))^2}{c^2} \right)} \quad (C.3)$$

where Q = total heat input from the welding arc

x = distance away from the weld centreline

t = welding time (t=0 start of weld)

r = time required by the centre of the heat source to reach the plane of analysis from its starting point

v = velocity of the welding arc

c = radius of the surface heat source

This "disc" source moves along the surface of the weldment in the x direction and deposits heat on the reference plane.

Such a surface heat source model is ideal for low penetration welds. For deeper penetration welds, assuming a surface heat source results in unrealistically high temperatures on the surface. This problem arises because the surface heat source model fails to account for the digging action of the arc and the convective heat flow towards tail end of the weld pool.

C.3 HEMISPHERICAL GAUSSIAN VOLUME FLUX MODEL

One step toward a more realistic model is to distribute the heat input Q into a hemispherical volume [C.4]. The thermal flux distribution of such a heat source can be written as:

$$q(x,y,\xi) = \frac{6\sqrt{3}Q}{c^3\pi\sqrt{\pi}} e^{-3\left(\frac{x^2 + y^2 + \xi^2}{c^2}\right)} \quad (C.4)$$

where c = radius of the hemispherical heat source

y = distance from top surface of weldment

$\xi = z + v(\tau - t)$

For a 2D analysis the reference plane is usually assumed to be at $z = 0$.

This model can account for the digging action of the arc, but fails to include the convective heat flow from the arc to the tail end of the weld pool.

C.4 UNIFORM FLUX IN THE WELD POOL

Paley and Hibbert [C.5] first suggested distributing the heat input Q uniformly into the fusion zone while the arc passes through the plane of analysis in a 2D simulation. This approach was used in preliminary studies and yielded reasonably good results far away from the fusion zone, but not in the vicinity of the weld pool. This is due to the fact that this model, which is similar to a hemispherical volume source model, fails to account for the convective heat flow in the weld pool along the weld direction. Also the duration of heating will greatly affect the results; a large heating time usually results in a long and narrow weld pool while a small heating time results in a short and wide weld pool. Thus it will be very difficult to accurately predict the weld pool shape using such a heat source model.

The next logical step therefore is to distribute the heat uniformly into the weld pool, so that as the welding arc passes the plane of reference, a uniform body heat flux will be released by the elements inside the weld pool. These elements are termed "heating elements". Applying this model to a 2-D heat flow analysis requires the following preliminary steps:

1. estimate the weld pool geometry either experimentally or analytically [C.6].
2. divide the weld pool along the weld line into time zones.
3. within each time zone, a group of elements most representative of the weld pool cross-sectional area is selected as heating elements. (For steps 1, 2 & 3 see figure C.2.)
4. calculate the approximate weld pool volume as

$$VOL = \sum_{i=1}^n AREA_i * t_i * VEL \quad (C.5)$$

where VOL = volume of the weld pool

i = time zone i

n = total # of time zones

t_i = time period of zone i

VEL = travel speed of the arc

$AREA_i$ = Cross sectional area of all heating elements in the time zone i

5. the uniform body flux BF can be calculated as

$$BF = \frac{Q}{VOL} \text{ J/cm /sec} \quad (C.6)$$

6. Apply the above BF to the designated heating elements for each time period during the entire heating time of the arc.

The authors found two problems with this model. One was that the finite element mesh must be generated to coincide with the weld pool shape. Therefore each new finite element analysis will require a new finite element mesh. This is likely as most weld pool geometries are significantly different. The second problem found with this model was that the predicted weld pool geometry resulting from the temperature analysis is larger than the one measured experimentally even though the heat input was distributed into the experimentally measured weld pool geometry. This indicates that the heat input should be deposited into a weld pool smaller than the actual one, which is unrealistic, or that some form of a heat flux distribution in the weld pool such as a Gaussian distribution should be used.

C.5 DOUBLE ELLIPSOIDAL MODEL

Two ellipsoids with a Gaussian heat flux distribution have been used to simulate the heat source of a weld arc by Goldak et.al. [C.4]. The front half of the heat source is the quadrant of one ellipsoid and the rear half is the quadrant of another ellipsoid (figure C.3). The heat flux distribution in the front quadrant can be written as:

$$q_f(x, y, \xi) = \frac{12\sqrt{3}Q_f}{abc_f\pi\sqrt{\pi}} e^{-3\left(\frac{x^2}{a^2} + \frac{y^2}{b^2} + \frac{\xi^2}{c_f^2}\right)} \quad \xi > 0 \quad (C.7)$$

Similarly, the rear quadrant flux distribution can be written as:

$$q_r(x, y, \xi) = \frac{12\sqrt{3}Q_r}{abc_r\pi\sqrt{\pi}} e^{-3\left(\frac{x^2}{a^2} + \frac{y^2}{b^2} + \frac{\xi^2}{c_r^2}\right)} \quad \xi < 0 \quad (C.8)$$

where $\xi = v(r-t)$ in a 2-D analysis

v = travel speed of welding arc

t = time welding, $t=0$ corresponds to weld front reaching the plane of reference

τ = lag time for the arc to reach the plane of reference ($z=0$)

Q = heat input from welding arc

f_f = fraction of heat input to the front ellipsoidal

f_r = fraction of heat input to the rear ellipsoidal

$f_f + f_r = 1.0$

a = half width of weld pool

b = depth of weld pool

c_f = length of weld pool between the advancing front and the arc

c_r = length of weld pool between the arc and the tail end

(x,y,ξ) = moving coordinates

Variables a , b , c_f , c_r are weld pool geometry parameters which can be measured experimentally or estimated analytically.

To apply this heat source model to a 2-D finite element heat flow analysis, one must define those elements inside the fusion zone as "heating elements". During welding the heat flux distribution of these elements can be calculated from equation (C.7) or (C.8) depending on ξ . By carefully adjusting the distribution factors f_f and f_r , one can obtain the desired weld pool shape. To model a different weld pool shape, one need only define a new set of weld pool geometry parameters a , b , c_r , c_f , f_r , f_f , but no change to the finite element mesh is necessary. This model is ideal for modelling multi-pass welds for the above reason.

REFERENCES

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- C.2 Pavelic, V., Tanbakuchi, R., Uyehara, O.A., and Meyers, P.S., "Experimental and Computed Temperature Histories in Gas-Tungsten-Arc Welding of Thin Plates", Welding Journal, Research Supplement, Vol. 48, 1969, pp. 295s-305s.
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- C.4 Goldak, J., Chakravarti, A., and Bibby, M., "An Advanced Finite Element Model for Welding Heat Sources", Department of Mechanical and Aeronautical Engineering, Carlton University, Ottawa, January 1983.
- C.5 Paley, Z. and Hibbert, P.D., "Computation of Temperatures in Actual Weld Designs", Welding Journal, Research Supplement, Vol. 54, 1975, pp. 385s-392s.
- C.6 Christensen, N., Davies V. de L., and Gjermundsen, K., "Distribution of Temperatures in Arc Welding", British Welding Journal, February 1965, pp. 54-75.

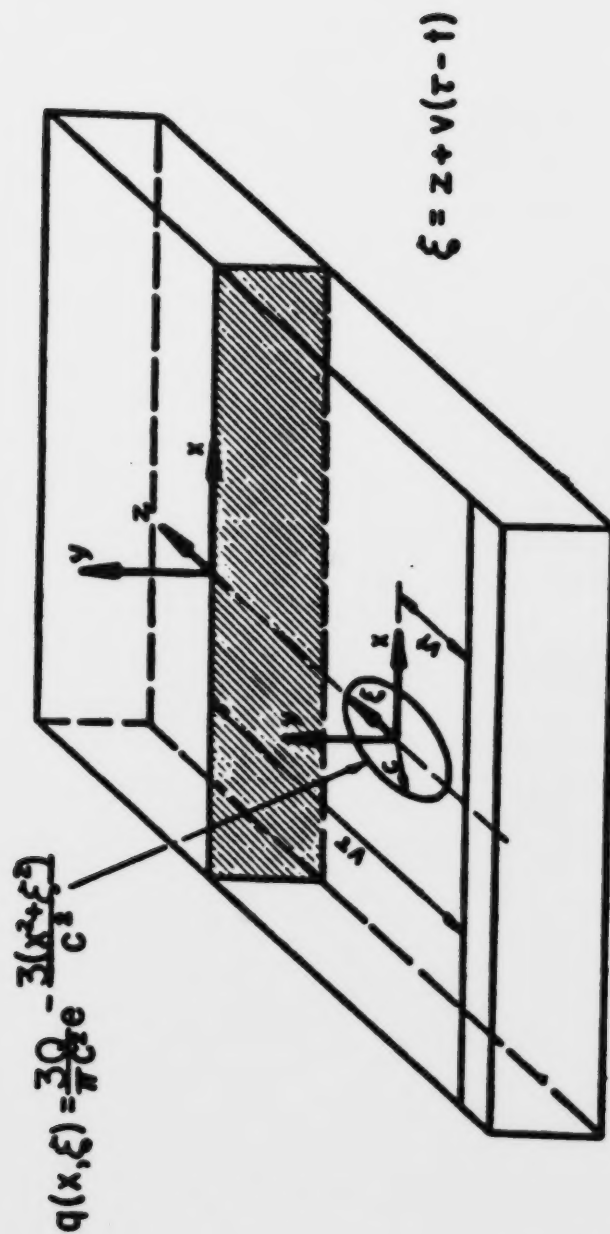


FIGURE C.1. CIRCULAR DISC SURFACE HEAT SOURCE

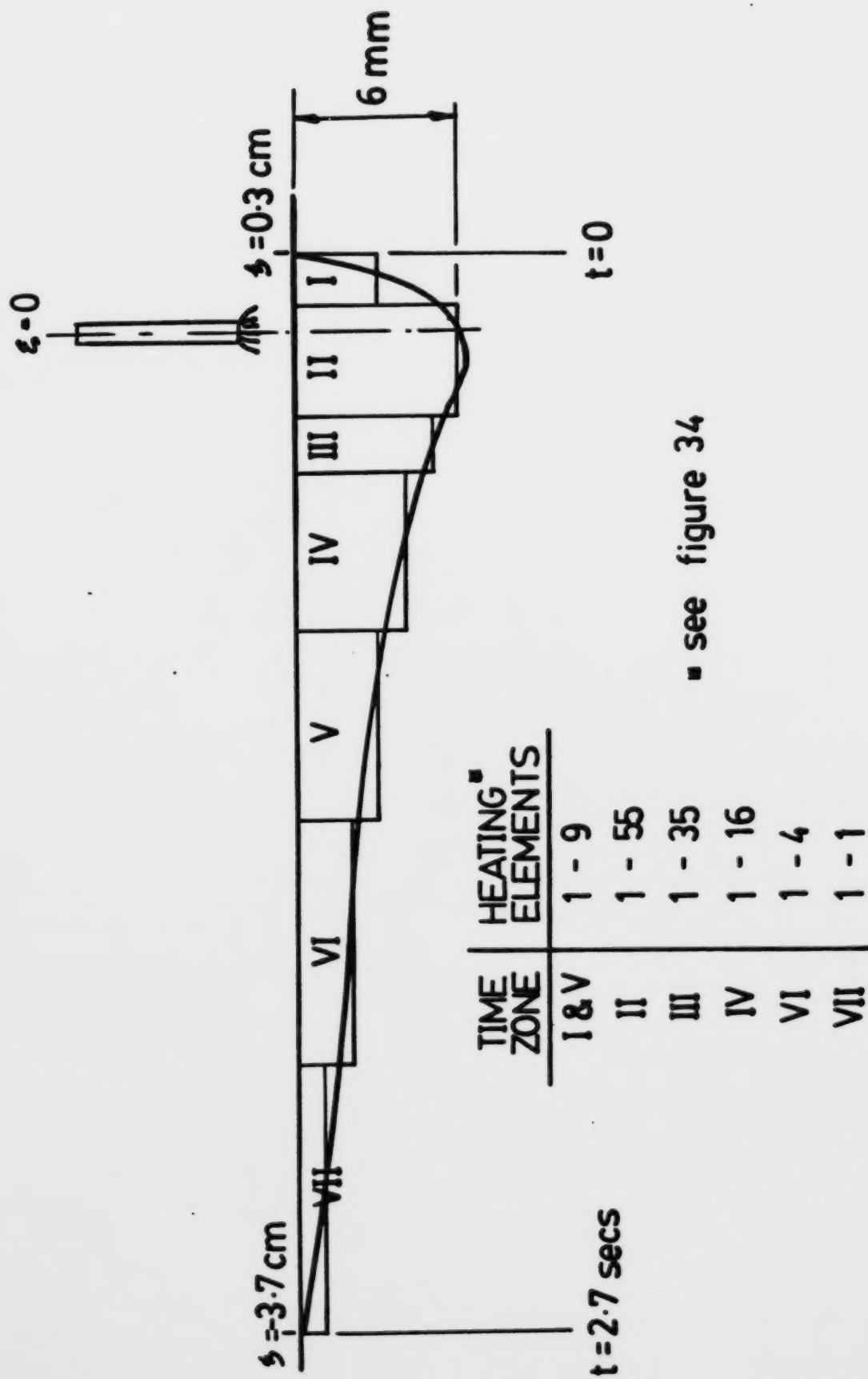


FIGURE C.2. UNIFORM HEAT FLUX DISTRIBUTION IN THE WELD POOL
-HEATING ELEMENTS SELECTION

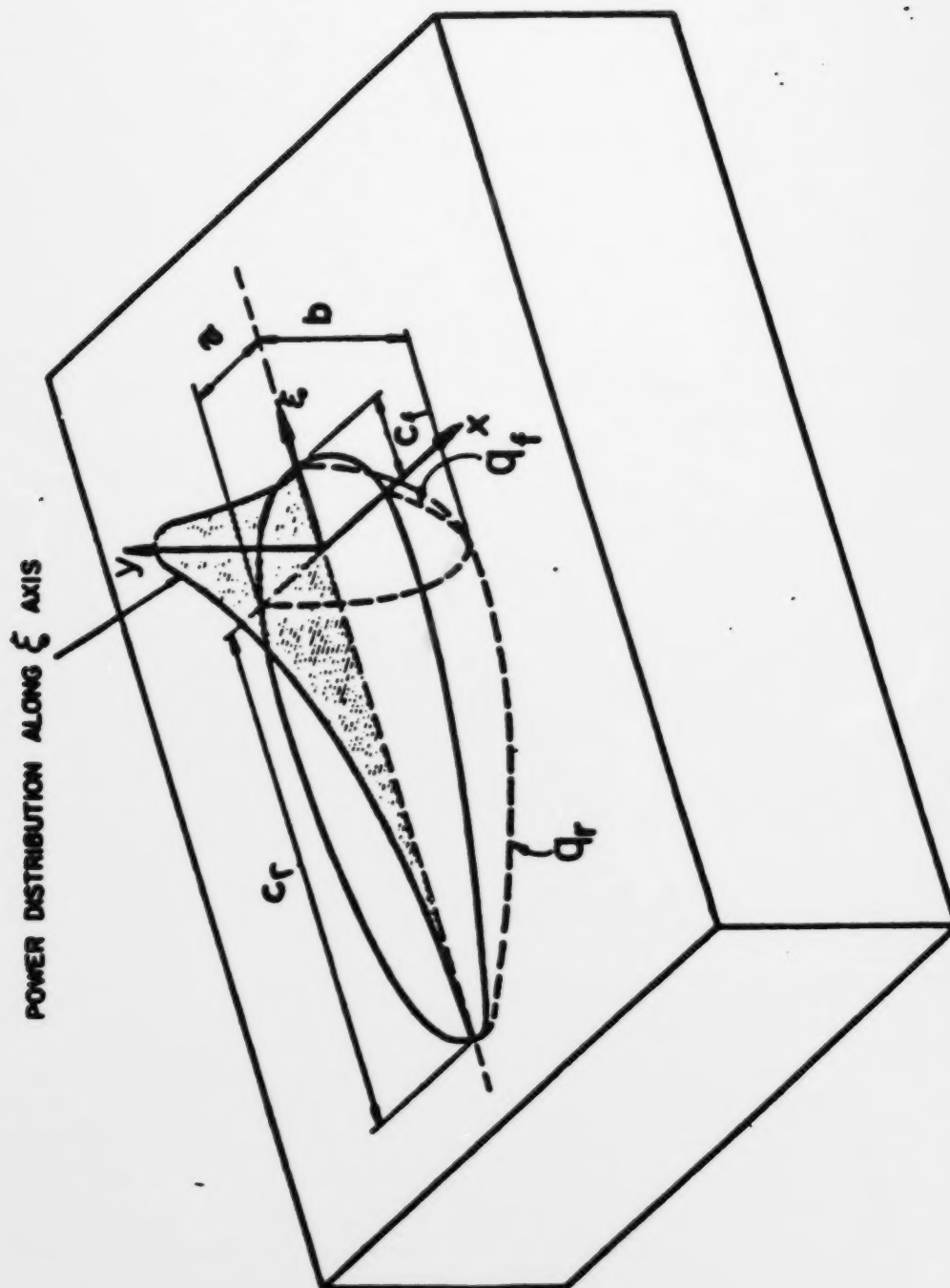


FIGURE C.3. DOUBLE ELLIPSOIDAL HEAT SOURCE MODEL

APPENDIX D

DETERMINATION OF
CONVECTIVE HEAT TRANSFER COEFFICIENTS
AND WELD ARC EFFICIENCY

Appendix D

DETERMINATION OF CONVECTIVE HEAT TRANSFER COEFFICIENTS AND WELD ARC EFFICIENCY

D.1 INTRODUCTION

In order to assist in understanding the amount of heat energy entering and leaving a stainless steel plate in submerged arc welding, studies were undertaken on heated and welded plates with various arrangements of thermocouples and insulation. This helped to determine the behaviour of the heat transfer coefficients on the horizontal plate surface as well as determining experimental values for weld arc efficiency. This study is described extensively in [D.1] and will be summarized below.

D.2 HEATED PLATE TEST

D.2.1 Test Procedure

A mild steel plate 760 mm x 250 mm x 12.7 mm thick had a cylindrical heating element inserted into a hole in the mid-plane along the centre-line. Twelve thermocouples were mounted on the plate surface at locations as shown in figure D.1 for the recording of the temperature profile along a plane perpendicular to the heated centreline. 837 watts of power were supplied to the heating element over a sufficient period of time so that a steady state condition was established. This power supply was chosen so that the resulting steady state temperature profile is similar to a typical temperature profile produced on a plate heated by a central weld.

The test was intended to be conducted in two parts. The first part was conducted with thermocouples on the upward facing surface and the

downward facing surface insulated. This will allow for the estimation of heat transfer coefficients of a upward facing non-isothermal surface. Similarly, the second part was to be conducted with an insulated top surface and thermocouples on the bottom surface for the estimation of heat transfer coefficients of a downward facing non-isothermal surface. Unfortunately, the heating element was damaged after the first part of the test, thus the h_c of downward facing surface had to be approximated by other means which will be discussed later.

The basic procedure for the estimation of heat transfer coefficients from the heated plate test were,

- 1) record the steady state temperature history along a plane perpendicular to the weld (see figure D.2).
- 2) obtain an expression of temperature as a function of the distance from the weld, $T(x)$, using least square technique.
- 3) obtain the first derivative $T'(x)$ by differentiating the function $T(x)$.
- 4) divide the plane perpendicular to the centreline into control volumes (see figure D.3). Only half of the plate need to be considered because of symmetry.
- 5) perform an energy balance over the control volume at a location x . For a steady state condition,

HEAT IN = HEAT OUT

$$kAT'(x + \frac{\Delta x}{2}) = kAT'(x - \frac{\Delta x}{2}) + [h_r(T) + h_c(T)] [T(x) - 20^\circ\text{C}] A_s \quad (\text{D.1})$$

where k = conductivity of mild steel

h_r = equivalent radiation heat transfer coefficient

h_c = convective heat transfer coefficient

A_s = surface area

A = cross-sectional area

6) calculate $h_c(T)$ from equation D.1 as,

$$h_c(T) = \frac{k A [T'(x + \frac{\Delta x}{2}) - T'(x - \frac{\Delta x}{2})]}{A_s [T(x) - 20^\circ\text{C}]} - h_r(T) \quad (\text{D.2})$$

The program that performs the above task is listed in [D.1].

D.2.2 Test Results - Convective Heat Transfer Coefficients

For the upward facing surface the estimated h_c is as shown in figure D.4. A maximum h_c of $26 \text{ W/m}^2\text{K}$ was observed at 320°C . Above this temperature a gradual decline of h_c occurred possibly due to the reduction of pressure driven flow because of the symmetry of the temperature profile near the centreline. A sharper decline of h_c occurred below 300°C and reached a minimum of about $10 \text{ W/m}^2\text{K}$. The h_c value began to rise again as the temperature continued to drop. This increase can be attributed to the pressure driven flow near the edge of the plate as suggested by Yousef [D.2]. If one ignores the edge and symmetry effects, one can assume a tri-linear temperature dependent h_c as shown in figure D.4. This linear approximated h_c can be used for finite element heat flow analysis.

As for the h_c of a downward facing isothermal surface, it is estimated to be one half of the upward facing one according to the experiments of Hassan et.al. [D.3]. For non-isothermal surfaces however, pressure gradient driven flow also exists on a downward facing surface thus the ratio is expected to be greater than one half. Due to the lack of data the downward facing h_c has been assumed $3/4$ that of the upward facing h_c .

D.3 WELDED PLATE TEST

D.3.1 Test Procedure

A test similar to that described above except the heat source was a welding arc and the plate was fully insulated by insulation and weld flux (see figure D.5). Therefore all heat input into the plate is conducted away from the weld bead. The temperature data recorded at the 16 thermocouple locations was used to calculate the heat flow rate Q through a section just outside the insulated region. Integrating Q with respect to time from the start of welding to infinity the total heat input absorbed by the plate was determined. The arc efficiency was then calculated by dividing this heat input by the actual power supplied to the welding arc.

D.3.2 Test Results - Weld Arc Efficiency

Initially tests were made with mild steel plate. These tests indicated a weld efficiency of 93% averaged from the results of 3 sets of thermocouples. This agrees well with the range of 90 to 99% quoted in the literature.

A similar approach was used with data produced by Leung [D.4] using 316L stainless steel plates. A weld efficiency of 90% was calculated.

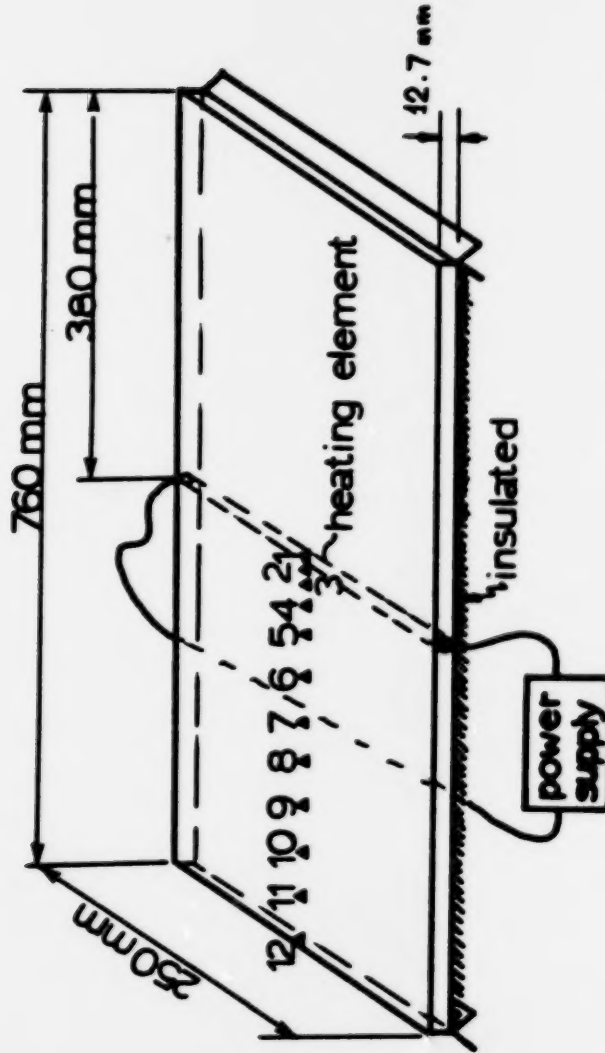
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- D.4 Leung, K., "Study of Finite Element Modelling of Multipass Welds", Dept. of Mech. Eng., University of Waterloo Report, April 1983.

**THERMOCOUPLE
LOCATIONS**

1 2 3 4 5 6 7 8 9 10 11 12

0
5
10
20
30
100
150
200
250
300
350
380
mm



D-7

FIGURE D.1. HEATED PLATE TEST SETUP

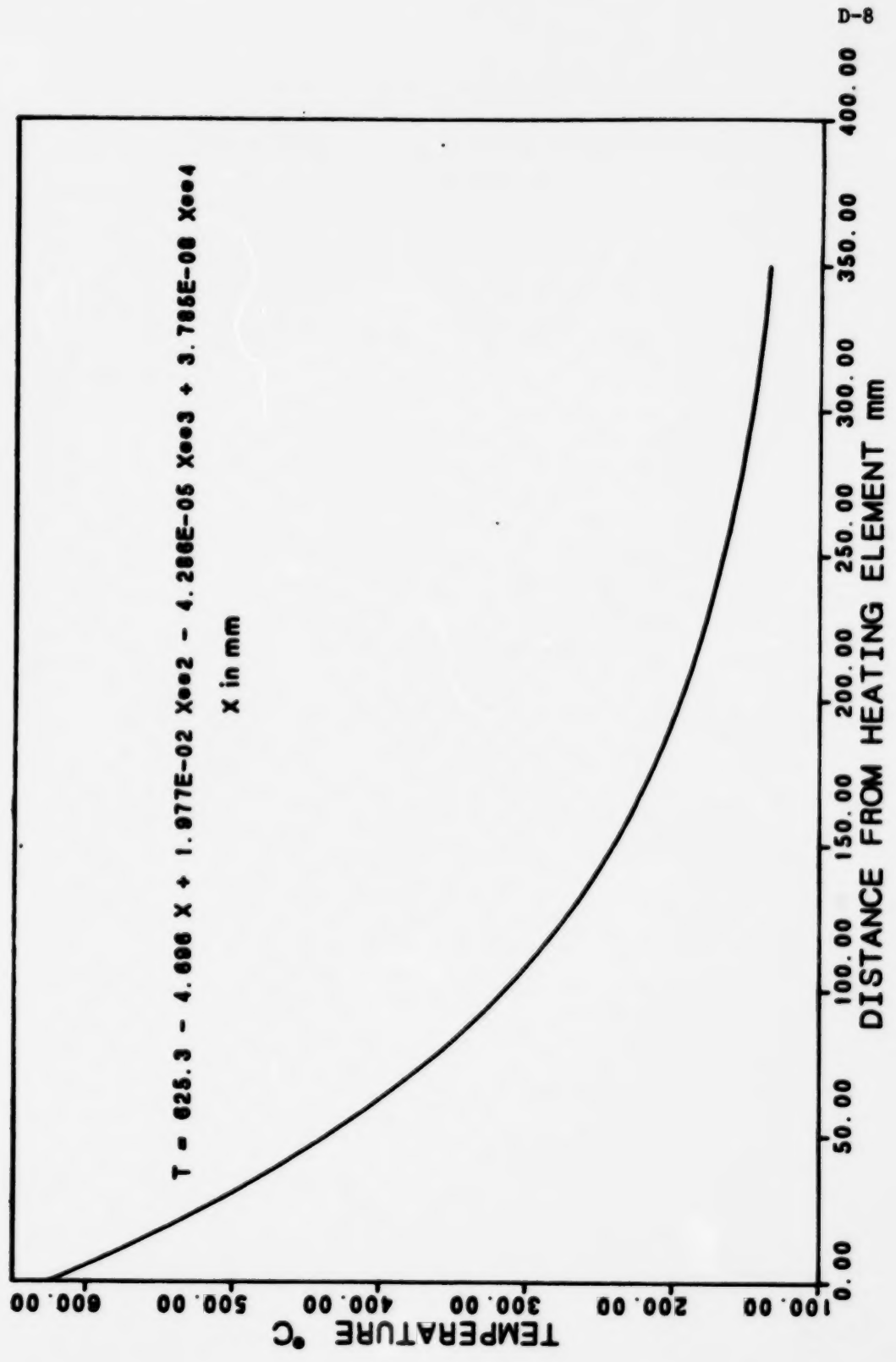
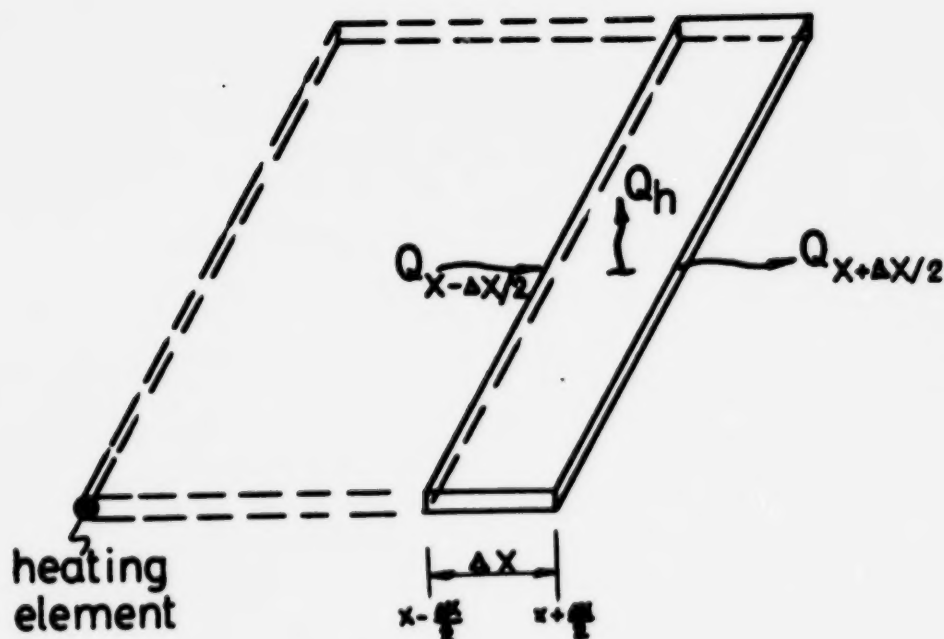


FIGURE D.2. TEMPERATURE PROFILE OF A CENTRALLY HEATED PLATE



Q_h = convection & radiation heat loss

$Q_{x \pm \frac{\Delta x}{2}}$ = heat conduction through $x \pm \frac{\Delta x}{2}$ face

FIGURE D.3. A TYPICAL CONTROL VOLUME FOR THE ESTIMATION OF HEAT TRANSFER COEFFICIENT

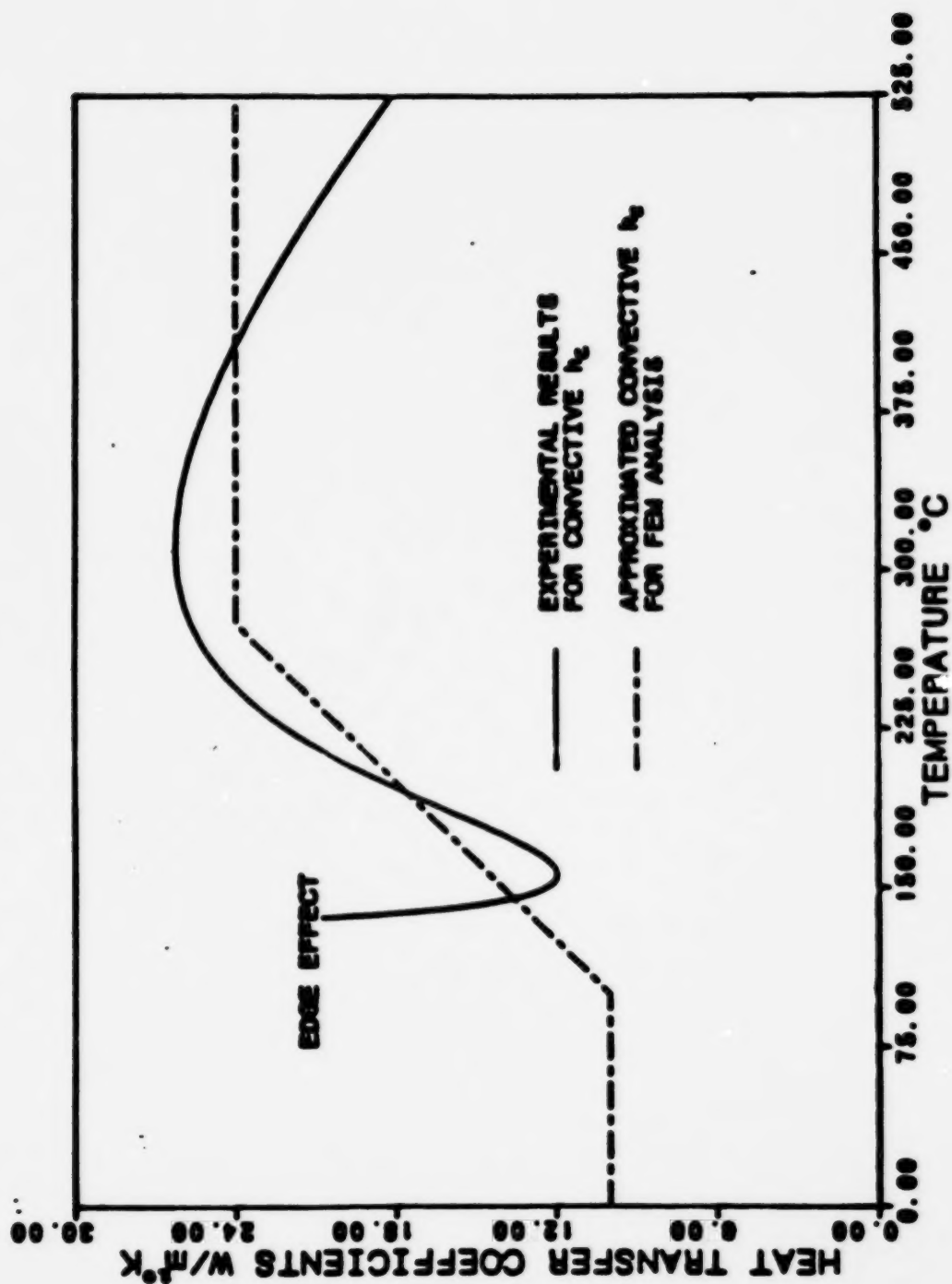


FIGURE D.4. HEAT TRANSFER COEFFICIENTS ON A WELDMENT SURFACE
FOR RADIATION CONVECTION

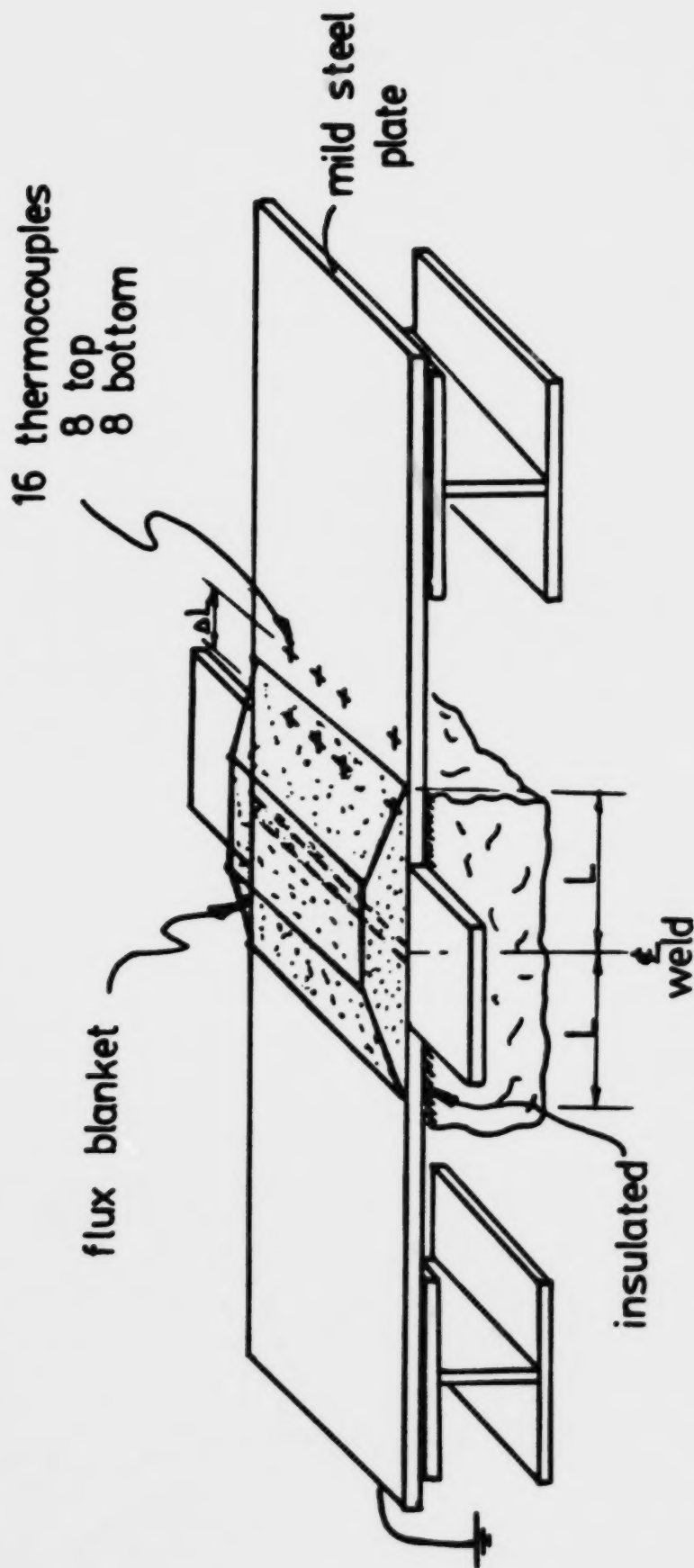


FIGURE D.5. EXPERIMENTAL SET UP FOR MEASURING ARC EFFICIENCIES OF SAW WELDING

APPENDIX E

ABAQUS INPUT DATA

SINGLE PASS WELD ANALYSES

E.1 HEAT TRANSFER ANALYSIS INPUT DATA

```

*HEADING
*****
**  HEAT TRANSFER ANALYSIS FOR 316L STAINLESS STEEL PLATE  *
**  DURING SINGLE PASS SUBMERGED ARC WELDING              *
*****
**
**
*****
**  MODEL DEFINITION
*****
**
*****
**  NODE GENERATION **
*****
*NODE, NSET=ALL
101, 0. 0, 1. 38
106, 0. 33, 1. 37
109, 0. 60, 1. 32
110, 0. 75, 1. 32
120, 2. 0, 1. 32
136, 6. 0, 1. 32
144, 10. 0, 1. 32
152, 30. 0, 1. 32
601, 0. 0, 0. 95
606, 0. 25, 1. 06
607, 0. 29, 1. 03
609, 0. 37, 0. 935
610, 0. 44, 0. 82
914, 0. 65, 0. 0
915, 0. 95, 0. 0
916, 1. 25, 0. 0
917, 1. 45, 0. 0
918, 1. 65, 0. 0
919, 1. 825, 0. 00
920, 2. 00, 0. 0
936, 6. 0, 0. 0
944, 10. 0, 0. 0
952, 30. 0, 0. 0
1014, 0. 35, 0. 00
1107, 0. 0, 0. 89
1109, 0. 0, 0. 77
1110, 0. 0, 0. 65
1114, 0. 0, 0. 0
*NGEN, LINE=P, NSET=ALL
101, 106, 1, , 0. 15, 1. 375
106, 109, 1, , 0. 465, 1. 35
*NGEN, NSET=ALL
601, 606, 1
101, 601, 100
102, 602, 100
103, 603, 100
104, 604, 100
105, 605, 100

```

106, 606, 100
110, 120, 1
120, 920, 100
607, 609, 1
1107, 1109, 1
1110, 1114, 1
*NGEN, LINE=P, NSET=ALL
107, 1107, 100, 607
108, 1108, 100, 608
109, 1109, 100, 609
110, 1110, 100, 610
*NGEN, NSET=ALL
910, 914, 1
1010, 1014, 2
136, 936, 200
144, 944, 400
152, 952, 400
120, 136, 1
320, 336, 2
520, 536, 1
720, 736, 2
920, 936, 1
136, 144, 1
536, 544, 2
936, 944, 1
144, 152, 1
544, 552, 2
944, 952, 1
*NGEN, LINE=P, NSET=ALL
510, 520, 1, , 1. 30, 0. 7
111, 911, 200, 511
112, 912, 100, 512
113, 913, 200, 513
114, 914, 100, 514
115, 915, 200, 515
116, 916, 100, 516
117, 917, 200, 517
118, 918, 100, 518
119, 919, 200, 519
120, 920, 100, 520

** ELEMENT GENERATION **

*ELEMENT, TYPE=DC2D4
1, 101, 201, 202, 102
2, 102, 202, 203, 103
5, 103, 203, 204, 104
10, 104, 204, 205, 105
17, 105, 205, 206, 106
26, 106, 206, 207, 107
31, 606, 605, 707, 607
32, 605, 604, 807, 707
33, 604, 603, 907, 807
34, 603, 602, 1007, 907
35, 602, 601, 1107, 1007
*ELGEN

```

2, 2, 100, 1
3, 2, -1, 1
5, 3, 100, 1
7, 3, -1, 1
10, 4, 100, 1
13, 4, -1, 1
17, 5, 100, 1
21, 5, -1, 1
26, 4, 1, 10
26, 5, 100, 1
36, 10, 100, 1
46, 10, 100, 1
56, 10, 100, 1
*ELEMENT, TYPE=DC2D8
66, 110, 310, 312, 112, 210, 311, 212, 111
88, 120, 520, 522, 122, 320, 521, 322, 121
104, 136, 936, 938, 138, 536, 937, 538, 137
*ELGEN
66, 5, 200, 1
66, 2, 2, 5
71, 5, 200, 1
71, 2, 2, 5
76, 3, 2, 4
76, 4, 200, 1
80, 4, 200, 1
84, 4, 200, 1
88, 2, 400, 1
88, 8, 2, 2
89, 8, 2, 2
104, 8, 2, 1
*****
** DEFINE ELEMENT SET AND NODE SET **
*****
*ELSET, ELSET=TOP, GENERATE
94, 102, 2
104, 111, 1
*ELSET, ELSET=BOT, GENERATE
79, 87, 4
89, 103, 2
104, 111, 1
*ELSET, ELSET=HEAT1, GENERATE
1, 55, 1
*ELSET, ELSET=PLOT, GENERATE
1, 87, 1
*NSET, NSET=V1, GENERATE
210, 1010, 200
*NSET, NSET=V2, GENERATE
110, 910, 200
*NSET, NSET=V3, GENERATE
310, 1110, 200
*****
** PLATE THICKNESS (cm) **
*****
*ATTRIBUTE
1.00
*****

```

** MULTIPLE POINT CONSTRAINTS (MESH REDUCTION INTERFACE) **

*EQUATION

3

V1, 11, 1. 0, V2, 11, -0. 5, V3, 11, -0. 5

*MPC

2, 220, 120, 320, 520

2, 420, 120, 320, 520

2, 620, 520, 720, 920

2, 820, 520, 720, 920

2, 336, 136, 536, 936

2, 736, 136, 536, 936

 ** MATERIAL PROPERTIES OF 316L STAINLESS STEEL PLATE **

*MATERIAL

*CONDUCTIVITY

** W/cm K

0. 1274, -23. 0

0. 3136, 1375. 0

1. 568, 1400. 0

*SPECIFIC HEAT

** W/sec g K

0. 435, -23. 0

0. 456, 27. 0

0. 489, 127. 0

0. 519, 227. 0

0. 544, 327. 0

0. 561, 427. 0

0. 619, 827. 0

0. 732, 1375. 0

*DENSITY

** g/cm3

7. 952

** LATENT HEAT

** J/g

292. 0, 1375. 0, 1400. 0

** DEFINE INITIAL TEMPERATURES **

*INITIAL CONDITIONS, TYPE=TEMPERATURE

ALL, 20. 0

**

**

** HISTORY DEFINITION

**

*RESTART, WRITE

**

*STEP, AMP=RAMP, INC=100

*HEAT TRANSFER, TEMPTOL=5. 00, END=PERIOD, DELTMX=500. 0

0. 150, 2. 700

*DFLUX, OP=NEW

HEAT1, BFNU

*FILM

TOP, F4NU
BOT, F2NU
75, F3NU
111, F3NU
*RADIATE, ZERO=-273. 16
TOP, R4, 20. 0, 3. 12E-12
BOT, R2, 20. 0, 3. 12E-12
75, R3, 20. 0, 3. 12E-12
111, R3, 20. 0, 3. 12E-12
*NODE PRINT, NSET=ALL, FREQ=100
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=50
*HEAT TRANSFER, TEMPTOL=2. 00, END=PERIOD, DELTMX=200. 0
0. 50, 297. 3
*DFLUX, OP=NEW
8, S4, 0. 00
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=50
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=50
*HEAT TRANSFER, TEMPTOL=1. 00, END=PERIOD, DELTMX=50. 0
50. 0, 1500. 0
*DFLUX, OP=NEW
8, S4, 0. 00
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=50
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP

E. 2 USER DEFINED SUBROUTINES FOR HEAT TRANSFER ANALYSIS

E. 2. 1 Subroutine DFLUX

```

CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C   SUBROUTINE TO CALCULATE POWER DENSITY DISTRIBUTION          C
C   FOR SINGLE PASS WELD                                       C
C   (THIS SUBROUTINE IS CALLED BY ABAQUS DIRECTLY)              C
CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C
C   SUBROUTINE DFLUX(FLUX, TEMP, KSTEP, KINC, TIME, NOEL,
*                   NPT, COORDS, JLTYP)
C   IMPLICIT DOUBLE PRECISION (A-H, O-Z)
C   DIMENSION COORDS(3)
C
C--WELDING PARAMETERS
C
C   AETA=0. 900
C   VOLT=34. 0
C   CURR=450. 0
C   VEL =88. 9/60. 0
C
C--FRONT QUADRANT (ZETA. GE. 0. 0)
C
C   FF=0. 8
C   A1=0. 6
C   B1=0. 6
C   C1=0. 3
C
C--REAR QUADRANT (ZETA. LT. 0. 0)
C
C   FR=0. 2
C   A2=0. 6
C   B2=0. 6
C   C2=3. 7
C
C--HEAT SOURCE MODEL COORDINATES (X, Y, ZETA)
C
C   X=COORDS(1)
C   Y=COORDS(2)-1. 37
C   ZETA=C1-VEL*TIME
C
C--CALCULATE POWER DENSITY ALONG PLANES PARALLEL TO THE WELD
C
C   G=AETA*VOLT*CURR
C   IF(ZETA. LT. 0. 0) GO TO 20
C   CALL HEAT(FF, A1, B1, C1, G, X, Y, ZETA, FLUX)
C   GO TO 21
20 CALL HEAT(FR, A2, B2, C2, G, X, Y, ZETA, FLUX)
21 IF(Y. GT. 0. 0) FLUX=0. 0
C   RETURN
C   END

```

E. 2. 2 Subroutine HEAT

```

CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C   SUBROUTINE TO CALCULATE HEAT DISTRIBUTION                      C
C   USING GOLDAK'S MODEL                                           C
C   (THIS SUBROUTINE IS CALLED BY DFLUX)                           C
CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C
C   SUBROUTINE HEAT(F,A,B,C,G,X,Y,ZETA,FLUX)
C   IMPLICIT DOUBLE PRECISION (A-H,O-Z)

C   PIE=3.1415927
C   PP=-3.0*((X/A)**2 + (Y/B)**2 + (ZETA/C)**2 )
C   FLUX=12.0*SQRT(3.0)*F*G*EXP(PP)/(A*B*C*PIE*SQRT(PIE))
C   IF (PP.LT.-3.0) FLUX=0.0
C   RETURN
C   END

```

E. 2. 3 Subroutine FILM

```

CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C   SUBROUTINE TO CALCULATE HEAT TRANSFER COEFFICIENTS          C
C   (THIS SUBROUTINE IS CALLED BY ABAQUS DIRECTLY)              C
CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C
C   SUBROUTINE FILM(H,SINK,TEMP,KSTEP,KINC,TIME,NOEL,
C   *NPT,COORDS,JLTYP)
C   IMPLICIT DOUBLE PRECISION (A-H,O-Z)
C   DIMENSION COORDS(3)
C   SINK=20.0

C
C--TOP SURFACE
C
C   H=(10.0+14.0/175.0*(TEMP-100.0))/10000.0
C   IF (TEMP.GT.275.0) H=0.0024
C   IF (TEMP.LT.100.0) H=0.0010

C
C--BOTTOM SURFACE
C
C   IF(COORDS(2).EQ.0.0) H=H*0.75
C   RETURN
C   END

```


E.3 RESIDUAL STRESS ANALYSIS INPUT DATA

*HEADING

```
*****
**  RESIDUAL STRESS ANALYSIS FOR 316L STAINLESS STEEL PLATE *
**  SINGLE PASS WELD ANALYSIS                               *
```

```
*****
**
**  GENERALIZED PLANE STRAIN CONDITION FOR PLATE SECTION
**  PLATE CLAMPED ON ONE SIDE ONLY
**
```

```
*****
**  MODEL DEFINITION
*****
```

```
*****
**
```

```
*****
**  NODE GENERATION - PLATE **
*****
```

*NODE

```
101, 0. 0, 1. 38
106, 0. 33, 1. 37
109, 0. 60, 1. 32
110, 0. 75, 1. 32
120, 2. 0, 1. 32
136, 6. 0, 1. 32
144, 10. 0, 1. 32
152, 30. 0, 1. 32
601, 0. 0, 0. 95
606, 0. 25, 1. 06
607, 0. 290, 1. 03
609, 0. 37, 0. 935
610, 0. 44, 0. 82
914, 0. 65, 0. 0
916, 1. 25, 0. 0
918, 1. 75, 0. 0
920, 2. 00, 0. 0
936, 6. 0, 0. 0
944, 10. 0, 0. 0
952, 30. 0, 0. 0
1107, 0. 0, 0. 89
1109, 0. 0, 0. 77
1110, 0. 0, 0. 65
1114, 0. 0, 0. 0
```

*NGEN, LINE=P

```
101, 106, 1, , 0. 15, 1. 375
106, 109, 1, , 0. 465, 1. 35
```

*NGEN

```
601, 606, 1
101, 601, 100
102, 602, 100
103, 603, 100
104, 604, 100
105, 605, 100
106, 606, 100
110, 120, 2
120, 920, 200
```

```

607, 609, 1
1107, 1109, 1
1110, 1114, 2
*NGEN, LINE=P
107, 1107, 100, 607
108, 1108, 100, 608
109, 1109, 100, 609
110, 1110, 100, 610
*NGEN, NSET=ALL
910, 914, 2
136, 936, 400
120, 136, 2
520, 536, 2
920, 936, 2
136, 144, 2
936, 944, 2
144, 152, 2
944, 952, 2
*NGEN, LINE=P, NSET=ALL
510, 520, 2, , 1, 30, 0, 7
112, 912, 200, 512
114, 914, 200, 514
116, 916, 200, 516
*NGEN, NSET=ALL
118, 918, 200
120, 920, 200
*NSET, NSET=ALL
101, 103, 105, 107, 109, 110
301, 303, 305, 307, 309, 310
501, 503, 505, 507, 509, 510
601, 603, 605, 707, 709, 710
907, 909, 910, 1107, 1109, 1110
1112, 1114
*****
** NODE GENERATION - RIGID BLOCK **
*****
*NODE, NSET=BLOCK
2048, 20, 0, 0, 0
2050, 25, 0, 0, 0
2052, 30, 0, 0, 0
2248, 20, 0, -1, 0
2250, 25, 0, -1, 0
2252, 30, 0, -1, 0
*****
** NODE GENERATION - GENERALIZED PLANE STRAIN COORDINATES **
*****
*NODE, NSET=GPSC
3000, 0, 0, 0, 0
4000, 0, 0, 0, 0
*****
** ELEMENT GENERATION - PLATE (GENERALISED PLANE STRAIN) **
*****
*ELEMENT, TYPE=CQPE6H
1, 101, 301, 303, 103, 3000, 4000
2, 103, 303, 305, 105, 3000, 4000
3, 105, 305, 307, 107, 3000, 4000

```

```

7, 505, 605, 707, 507, 3000, 4000
9, 501, 601, 603, 503, 3000, 4000
10, 603, 907, 707, 605, 3000, 4000
11, 601, 1107, 907, 603, 3000, 4000
12, 107, 307, 309, 109, 3000, 4000
17, 109, 309, 310, 110, 3000, 4000
22, 110, 310, 312, 112, 3000, 4000
40, 118, 318, 320, 122, 3000, 4000
41, 318, 518, 520, 320, 3000, 4000
42, 518, 718, 720, 520, 3000, 4000
43, 718, 918, 922, 720, 3000, 4000
44, 320, 520, 522, 122, 3000, 4000
45, 520, 720, 922, 522, 3000, 4000
46, 122, 522, 524, 124, 3000, 4000
56, 132, 532, 534, 136, 3000, 4000
57, 532, 932, 934, 534, 3000, 4000
58, 534, 934, 936, 136, 3000, 4000
59, 136, 936, 938, 138, 3000, 4000
*ELGEN, ELSET=PLOT1
1, 2, 200, 3
2, 2, 200, 1
5, 2, 200, 1
9, 2, 2, -1
12, 5, 200, 1
17, 5, 200, 1
*ELSET, ELSET=PLOT1
7, 10, 11
*ELGEN, ELSET=PLOT1
22, 5, 200, 1
22, 2, 2, 5
27, 5, 200, 1
27, 2, 2, 5
32, 4, 200, 1
32, 2, 2, 4
36, 4, 200, 1
*ELGEN, ELSET=PLOT2
46, 2, 400, 1
46, 5, 2, 2
47, 5, 2, 2
59, 8, 2, 1
*ELSET, ELSET=PLOT2
40, 41, 42, 43, 44, 45, 56, 57, 58
*****
** ELEMENT GENERATION - RIGID BLOCK (PLANE STRAIN) **
*****
*ELEMENT, TYPE=CPE4, ELSET=BLOCKE
300, 2048, 2248, 2250, 2050
301, 2050, 2250, 2252, 2052
*****
** ELEMENT GENERATION - NO FRICTION INTERFACE **
*****
*ELEMENT, TYPE=INTER2, ELSET=INTER
200, 2048, 2050, 948, 950
201, 2050, 2052, 950, 952
*NORMAL
300, 2048, 0, 0, 1, 0, 0, 0

```

```

300, 2050, 0. 0, 1. 0, 0. 0
301, 2050, 0. 0, 1. 0, 0. 0
301, 2052, 0. 0, 1. 0, 0. 0
*****
** ELEMENT THICKNESS (cm) **
*****
*ATTRIBUTE
1. 00
*****
** DEFINE ELEMENT SET AND NODE SET **
*****
*NSET, NSET=LEFT, GENERATE
101, 501, 200
501, 601, 100
1107, 1109, 2
1110, 1112, 2
*ELSET, ELSET=ALLE, GENERATE
1, 66, 1
*****
** MATERIAL PROPERTIES OF 316L STAINLESS STEEL PLATE **
*****
*MATERIAL, ELSET=ALLE
*ELASTIC
2. 00E+07, 0. 255, 20. 0
1. 90E+07, 0. 260, 149. 0
1. 81E+07, 0. 290, 260. 0
1. 71E+07, 0. 340, 371. 0
1. 62E+07, 0. 300, 482. 0
1. 53E+07, 0. 320, 593. 0
1. 43E+07, 0. 310, 704. 0
1. 32E+07, 0. 240, 816. 0
2. 00E+06, 0. 240, 1375. 0
*PLASTIC, HARD=KIN
2. 330E+04, 0. 00, 20. 0
5. 130E+04, 0. 10, 20. 0
1. 765E+04, 0. 00, 149. 0
4. 425E+04, 0. 10, 149. 0
1. 489E+04, 0. 00, 260. 0
4. 023E+04, 0. 10, 260. 0
1. 345E+04, 0. 00, 371. 0
3. 753E+04, 0. 10, 371. 0
1. 234E+04, 0. 00, 482. 0
3. 502E+04, 0. 10, 482. 0
1. 089E+04, 0. 00, 593. 0
3. 231E+04, 0. 10, 593. 0
0. 920E+04, 0. 00, 704. 0
2. 922E+04, 0. 10, 704. 0
0. 750E+04, 0. 00, 816. 0
2. 589E+04, 0. 10, 816. 0
1. 000E+00, 0. 00, 1375. 0
1. 000E+00, 0. 10, 1375. 0
*EXPANSION, ZERO=20. 0
0. 1500E-04, 20. 0
0. 168E-04, 77. 0
0. 172E-04, 127. 0
0. 179E-04, 327. 0

```

```

0.184E-04, 527.0
0.191E-04, 727.0
0.208E-04, 1127.0
0.221E-04, 1400.0
*****
** MATERIAL PROPERTIES OF RIGID BLOCK **
*****
*MATERIAL, ELSET=BLOCKE
*ELASTIC
1.00E+30, 0.25
*****
** MATERIAL PROPERTIES OF INTERFACE ELEMENT **
*****
*MATERIAL, ELSET=INTER
*FRICTION
0.0, 0.0
*****
** APPLY BOUNDARY DISPLACEMENT CONSTRAINTS **
*****
*BOUNDARY
LEFT, 1, 1, 0.0
1114, 1, 2, 0.0
2248, 2, 2, 0.0
2250, 2, 2, 0.0
2252, 1, 2, 0.0
4000, 1, 2, 0.0
*****
** DEFINE INITIAL TEMPERATURES **
*****
*INITIAL CONDITIONS, TYPE=TEMPERATURE
ALL, 20.0
**
**
*****
** HISTORY DEFINITION
*****
**
*RESTART, WRITE, FREQ=10
*STEP, AMP=RAMP, INC=300, CYC=10
*STATIC, PTOL=10.0
0.1, 1800.0, 0.00001, 500.0
*TEMPERATURE, FILE=18, BSTEP=1(INC=1), ESTEP=3(INC=5)
ALL
*EL PRINT
1, 1, 1, 1, 1, 1
1, 1, 1, 1, 1
1, 1, 1, 1, 1, 1, 1, 1
*NODE PRINT
1, 1, 1, 1, 1, 1, 1, 1
*EL FILE, COORDS, TEMPS, FREQ=10
2, 2
*NODE FILE, FREQ=10
2, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=15, CYC=10

```

```
*STATIC, PTOL=10.0  
1800.0, 1800.0  
*TEMPERATURE  
ALL, 20.0  
*EL PRINT  
1, 1, 1, 1, 1, 1  
1, 1, 1, 1, 1  
1, 1, 1, 1, 1, 1, 1, 1  
*NODE PRINT  
1, 1, 1, 1, 1, 1, 1, 1  
*EL FILE, COORDS, TEMPS, FREQ=10  
2, 2  
*NODE FILE, FREQ=10  
2, 1, 1, 1, 1  
*END STEP
```

APPENDIX F

ABAQUS INPUT DATA

MULTI-PASS WELD ANALYSES:

TWO PASSES LAID VERTICALLY

F.1 HEAT TRANSFER ANALYSIS INPUT DATA

*HEADING

```
*****  
**  TEMPERATURE ANALYSIS FOR 316L STAINLESS STEEL PLATE  **  
**  MULTI-PASS WELD - TWO PASSES LAID VERTICALLY        **  
*****
```

**

**

```
*****
```

** MODEL DEFINITION

```
*****
```

**

```
*****
```

** NODE GENERATION **

```
*****
```

*NODE

```
1000, 0. 0, 0. 00  
9000, 0. 0, 1. 32  
1200, 20. 0, 0. 00  
9200, 20. 0, 1. 32  
1210, 21. 0, 0. 00  
5210, 21. 0, 0. 66  
1220, 22. 0, 0. 00  
5220, 22. 0, 0. 66  
9220, 22. 0, 1. 32  
1250, 25. 0, 0. 00  
5250, 25. 0, 0. 66  
9250, 25. 0, 1. 32  
3255, 25. 5, 0. 33  
5255, 25. 5, 0. 66  
7255, 25. 5, 0. 99  
1260, 26. 0, 0. 00  
3260, 26. 0, 0. 33  
5260, 26. 0, 0. 66  
7260, 26. 0, 0. 99  
9260, 26. 0, 1. 32  
1270, 27. 0, 0. 00  
3270, 27. 0, 0. 33  
5270, 27. 0, 0. 66  
7270, 27. 0, 0. 99  
9270, 27. 0, 1. 32  
2272, 27. 2, 0. 165  
3272, 27. 2, 0. 33  
4272, 27. 2, 0. 495  
6272, 27. 2, 0. 825  
7272, 27. 2, 0. 99  
8272, 27. 2, 1. 155  
1274, 27. 4, 0. 00  
2274, 27. 4, 0. 165  
3274, 27. 4, 0. 33  
4274, 27. 4, 0. 495  
5274, 27. 4, 0. 66  
6274, 27. 4, 0. 825  
7274, 27. 4, 0. 99
```


8274, 27. 4. 1. 155
9274, 27. 4. 1. 32
1290, 29. 0. 0. 000
2290, 29. 0. 0. 165
3290, 29. 0. 0. 33
4290, 29. 0. 0. 495
5290, 29. 0. 0. 66
6290, 29. 0. 0. 825
7290, 29. 0. 0. 99
8290, 29. 0. 1. 155
9290, 29. 0. 1. 32
7298, 29. 8. 0. 99
8298, 29. 75. 1. 155
9298, 29. 7. 1. 32
1300, 30. 0. 0. 00
2300, 30. 0. 0. 165
3300, 30. 0. 0. 33
4300, 30. 0. 0. 495
5300, 30. 0. 0. 66
6300, 30. 0. 0. 825
7300, 30. 0. 1. 025
8300, 30. 0. 1. 1725
9300, 30. 0. 1. 32
*NGEN, NSET=NSP1
1000, 1200, 25
9000, 9200, 25
1220, 1250, 10
5220, 5250, 10
9220, 9250, 10
1260, 1270, 5
3260, 3270, 5
5260, 5270, 5
7260, 7270, 5
9260, 9270, 5
1274, 1290, 2
2274, 2290, 2
3274, 3290, 2
4274, 4290, 2
5274, 5290, 2
6274, 6290, 2
7274, 7290, 2
8274, 8290, 2
9274, 9290, 2
1290, 1300, 1
2290, 2300, 1
3290, 3300, 1
4290, 4300, 1
5290, 5300, 1
6290, 6300, 1
7290, 7298, 1
7298, 7300, 1
8290, 8298, 1
9290, 9298, 1
*NGEN, NSET=NSP2
7298, 7300, 1
8298, 8300, 1

```
9298, 9300, 1
*NSET, NSET=ALL
NSP1, NSP2
*****
** ELEMENT GENERATION **
*****
*ELEMENT, TYPE=DC2D4, ELSET=MAT
1, 1000, 1025, 9025, 9000
2001, 1200, 1210, 5210, 9200
2101, 1210, 1220, 5220, 5210
2102, 5210, 5220, 9220, 9200
2201, 1220, 1230, 5230, 5220
2501, 1250, 3255, 5255, 5250
2502, 5250, 5255, 7255, 9250
2551, 1250, 1260, 3260, 3255
2552, 3255, 3260, 5260, 5255
2553, 5255, 5260, 7260, 7255
2554, 7255, 7260, 9260, 9250
2601, 1260, 1265, 3265, 3260
2701, 1270, 2272, 3272, 3270
2702, 3270, 3272, 4272, 5270
2703, 5270, 6272, 7272, 7270
2704, 7270, 7272, 8272, 9270
2721, 1270, 1274, 2274, 2272
2722, 2272, 2274, 3274, 3272
2723, 3272, 3274, 4274, 4272
2724, 4272, 4274, 5274, 5270
2725, 5270, 5274, 6274, 6272
2726, 6272, 6274, 7274, 7272
2727, 7272, 7274, 8274, 8272
2728, 8272, 8274, 9274, 9270
2741, 1274, 1276, 2276, 2274
2901, 1290, 1291, 2291, 2290
*ELGEN, ELSET=MAT
1, 8, 25, 250
2201, 3, 10, 100, 2, 4000, 1
2601, 2, 5, 50, 4, 2000, 1
2741, 8, 2, 20, 8, 1000, 1
2901, 10, 1, 10, 8, 1000, 1
*****
** DEFINE ELEMENT SET AND NODE SET **
*****
*ELSET, ELSET=TOP, GENERATE
1, 1751, 250
2102, 2402, 100
2554, 2654, 50
*ELSET, ELSET=BOT, GENERATE
1, 1751, 250
2001, 2401, 100
2551, 2651, 50
2721, 2881, 20
2901, 2991, 10
*ELSET, ELSET=PLOT1, GENERATE
2741, 2881, 20
2742, 2882, 20
2743, 2883, 20
```

2744, 2884, 20
2745, 2885, 20
2746, 2886, 20
2747, 2887, 20
2748, 2888, 20
2901, 2991, 10
2902, 2992, 10
2903, 2993, 10
2904, 2994, 10
2905, 2995, 10
2906, 2996, 10
2907, 2997, 10
2908, 2998, 10
*ELSET, ELSET=HEAT1, GENERATE
2903, 2993, 10
2904, 2994, 10
2905, 2995, 10
2906, 2996, 10
*ELSET, ELSET=HEAT2, GENERATE
2905, 2995, 10
2906, 2996, 10
2907, 2997, 10
2908, 2998, 10
*ELSET, ELSET=PASS2
2987, 2997, 2988, 2998
*ELSET, ELSET=PLOT2
PLOT1
PASS2

** PLATE THICKNESS (cm) **

*ATTRIBUTE
1.00

** MATERIAL PROPERTIES OF 316L STAINLESS STEEL PLATE **

*MATERIAL, ELSET=MAT
*CONDUCTIVITY
** W/cm K
0.1274, -23.0
0.3136, 1375.0
1.568, 1400.0
*SPECIFIC HEAT
** W/sec g K
0.435, -23.0
0.456, 27.0
0.489, 127.0
0.519, 227.0
0.544, 327.0
0.561, 427.0
0.619, 827.0
0.732, 1375.0
*DENSITY
** g/cm3
7.952
*LATENT HEAT

```

** J/g
292. 0, 1375. 0, 1400. 0
*****
** DEFINE INITIAL TEMPERATURES **
*****
*INITIAL CONDITIONS, TYPE=TEMPERATURE
ALL, 20. 0
**
**
*****
** HISTORY DEFINITION
*****
**
*RESTART, WRITE, FREQ=10
*****
** P A S S - 1 **
*****
*STEP, AMP=STEP
*HEAT TRANSFER, TEMPTOL=5. 00, END=PERIOD, DELTMX=500. 0
0. 0001, 0. 0001
*MODEL CHANGE, REMOVE
PASS2
*END STEP
**
*STEP, AMP=RAMP, INC=100
*HEAT TRANSFER, TEMPTOL=5. 00, END=PERIOD, DELTMX=500. 0
0. 150, 2. 700
*DFLUX, OP=NEW
HEAT1, BFNU
*FILM
TOP, F3NU
BOT, F1NU
*RADIATE, ZERO=-273. 16
TOP, R3, 20. 0, 3. 12E-12
BOT, R1, 20. 0, 3. 12E-12
*NODE PRINT, NSET=ALL, FREQ=100
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=50
*HEAT TRANSFER, TEMPTOL=2. 00, END=PERIOD, DELTMX=200. 0
0. 50, 297. 2999
*DFLUX, OP=NEW
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=50
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=50
*HEAT TRANSFER, TEMPTOL=1. 00, END=PERIOD, DELTMX=50. 0
50. 0, 5700. 0, 25. 0, 1000. 0
*DFLUX, OP=NEW
*FILM, OP=MOD

```

```
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=50
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*****
** P A S S - 2 **
*****
*STEP, AMP=STEP
*HEAT TRANSFER, TEMPTOL=5. 00, END=PERIOD, DELTMX=500. 0
0. 0001, 0. 0001
*MODEL CHANGE, INCLUDE
PASS2
*END STEP
**
*STEP, AMP=RAMP, INC=100
*HEAT TRANSFER, TEMPTOL=5. 00, END=PERIOD, DELTMX=500. 0
0. 150, 2. 700
*DFLUX, OP=NEW
HEAT2, BFNU
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=100
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=50
*HEAT TRANSFER, TEMPTOL=2. 00, END=PERIOD, DELTMX=200. 0
0. 50, 297. 2999
*DFLUX, OP=NEW
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=50
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=50
*HEAT TRANSFER, TEMPTOL=1. 00, END=PERIOD, DELTMX=50. 0
50. 0, 5700. 0, 25. 0, 1000. 0
*DFLUX, OP=NEW
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=50
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
```

F.2 USER DEFINED SUBROUTINES FOR HEAT TRANSFER ANALYSIS

F.2.1 Subroutine DFLUX

```

CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C   SUBROUTINE TO CALCULATE POWER DENSITY DISTRIBUTION      C
C   FOR TWO PASS WELD - TWO PASS LAID VERITCALLY          C
C   (THIS SUBROUTINE IS CALLED BY ABAQUS DIRECTLY)          C
CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C
C   SUBROUTINE DFLUX(FLUX, TEMP, KSTEP, KINC, TIME, NOEL,
*               NPT, COORDS, JLTYP)
C   IMPLICIT DOUBLE PRECISION (A-H, O-Z)
C   DIMENSION COORDS(3)
C
C--WELDING PARAMETERS
C
C   AETA=0.90
C   VOLT=34.0
C   CURR=450.0
C   VEL =88.9/60.0
C   FLUX=0.0D0
C
C--FRONT QUADRANT (ZETA.GE.0.0)
C
C   FF=0.8
C   A1=0.6
C   B1=0.6
C   C1=0.3
C
C--REAR QUADRANT (ZETA.LT.0.0)
C
C   FR=0.2
C   A2=0.6
C   B2=0.6
C   C2=3.7
C
C--CENTER LOCATION AND ORIENTATION OF GOLDAK'S MODEL IN MESH
C
C   IF(TIME.LE.6000.0) GO TO 991
C   GO TO 992
991  XC=30.0
C   YC=0.90
C   TI=TIME
C   X=COORDS(1)-XC
C   Y=COORDS(2)-YC
C   IF (Y.GT.0.01) GO TO 21
C   ZETA=C1-VEL*TI
C   GO TO 999
992  XC=30.0
C   YC=1.31
C   TI=TIME-6000.0
C   X=COORDS(1)-XC
C   Y=COORDS(2)-YC
C   ZETA=C1-VEL*TI

```

```

C
C--CALCULATE POWER DENSITY
C
999  G=AETA*VOLT*CURRE
      IF(ZETA.LT.0.0) GO TO 20
      CALL HEAT(FF,A1,B1,C1,G,X,Y,ZETA,FLUX)
      GO TO 21
20   CALL HEAT(FR,A2,B2,C2,G,X,Y,ZETA,FLUX)
21   RETURN
      END

```

F.2.2 Subroutine HEAT

```

CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C  SUBROUTINE TO CALCULATE HEAT DISTRIBUTION                      C
C  USING GOLDAK'S ELLIPSOIDAL MODEL                              C
C  (THIS SUBROUTINE IS CALLED BY DFLUX)                          C
CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C
      SUBROUTINE HEAT(F,A,B,C,G,X,Y,ZETA,FLUX)
      IMPLICIT DOUBLE PRECISION (A-H,O-Z)
C
      PIE=3.1415927
      PP=-3.0*((X/A)**2 + (Y/B)**2 + (ZETA/C)**2)
      FLUX=12.0*SQRT(3.0)*F*G*EXP(PP)/(A*B*C*PIE*SQRT(PIE))
      IF(PP.LT.-3.0) FLUX=0.0
      RETURN
      END

```

F.2.3 Subroutine FILM

```

CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C  SUBROUTINE TO CALCULATE HEAT TRANSFER COEFFICIENTS          C
C  (THIS SUBROUTINE IS CALLED BY ABAGUS DIRECTLY)              C
CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C
      SUBROUTINE FILM(H,SINK,TEMP,KSTEP,KINC,TIME,NOEL,
      *NPT,COORDS,JLTYP)
      IMPLICIT DOUBLE PRECISION (A-H,O-Z)
      DIMENSION COORDS(3)
      SINK=20.0
C
C--TOP SURFACE
C
      H=(10.0+14.0/175.0*(TEMP-100.0))/10000.0
      IF (TEMP.GT.275.0) H=0.0024
      IF (TEMP.LT.100.0) H=0.0010
C
C--BOTTOM SURFACE
C
      IF(COORDS(2).EQ.0.0) H=H*0.75
      RETURN
      END

```


F.3 RESIDUAL STRESS ANALYSIS INPUT DATA

F.3.1 Pass 1

*HEADING

```
*****
**  RESIDUAL STRESS ANALYSIS FOR 316L STAINLESS STEEL PLATE *
**  MULTI-PASS WELD - TWO PASSES LAID VERTICALLY             *
```

```
*****
**
**  GENERALIZED PLANE STRAIN CONDITION FOR PLATE SECTION
**  PLATE CLAMPED ALONG BOTH EDGES
**
```

```
*****
**  MODEL DEFINITION
*****
```

```
**
*****
**  NODE GENERATION **
*****
```

*NODE

```
1000, 0. 0. 0. 00
9000, 0. 0. 1. 32
1200, 20. 0. 0. 00
9200, 20. 0. 1. 32
1210, 21. 0. 0. 00
5210, 21. 0. 0. 66
1220, 22. 0. 0. 00
5220, 22. 0. 0. 66
9220, 22. 0. 1. 32
1250, 25. 0. 0. 00
5250, 25. 0. 0. 66
9250, 25. 0. 1. 32
3255, 25. 5. 0. 33
5255, 25. 5. 0. 66
7255, 25. 5. 0. 99
1260, 26. 0. 0. 00
3260, 26. 0. 0. 33
5260, 26. 0. 0. 66
7260, 26. 0. 0. 99
9260, 26. 0. 1. 32
1270, 27. 0. 0. 00
3270, 27. 0. 0. 33
5270, 27. 0. 0. 66
7270, 27. 0. 0. 99
9270, 27. 0. 1. 32
2272, 27. 2. 0. 165
3272, 27. 2. 0. 33
4272, 27. 2. 0. 495
6272, 27. 2. 0. 825
7272, 27. 2. 0. 99
8272, 27. 2. 1. 155
1274, 27. 4. 0. 00
2274, 27. 4. 0. 165
3274, 27. 4. 0. 33
4274, 27. 4. 0. 495
```


5274, 27. 4, 0. 66
6274, 27. 4, 0. 825
7274, 27. 4, 0. 99
8274, 27. 4, 1. 155
9274, 27. 4, 1. 32
1290, 29. 0, 0. 000
2290, 29. 0, 0. 165
3290, 29. 0, 0. 33
4290, 29. 0, 0. 495
5290, 29. 0, 0. 66
6290, 29. 0, 0. 825
7290, 29. 0, 0. 99
8290, 29. 0, 1. 155
9290, 29. 0, 1. 32
7298, 29. 8, 0. 99
8298, 29. 75, 1. 155
9298, 29. 7, 1. 32
1300, 30. 0, 0. 00
2300, 30. 0, 0. 165
3300, 30. 0, 0. 33
4300, 30. 0, 0. 495
5300, 30. 0, 0. 66
6300, 30. 0, 0. 825
7300, 30. 0, 1. 025
8300, 30. 0, 1. 1725
9300, 30. 0, 1. 32
*NGEN, NSET=NSP1
1000, 1200, 25
9000, 9200, 25
1220, 1250, 10
5220, 5250, 10
9220, 9250, 10
1260, 1270, 5
3260, 3270, 5
5260, 5270, 5
7260, 7270, 5
9260, 9270, 5
1274, 1290, 2
2274, 2290, 2
3274, 3290, 2
4274, 4290, 2
5274, 5290, 2
6274, 6290, 2
7274, 7290, 2
8274, 8290, 2
9274, 9290, 2
1290, 1300, 1
2290, 2300, 1
3290, 3300, 1
4290, 4300, 1
5290, 5300, 1
6290, 6300, 1
7290, 7298, 1
7298, 7300, 1
8290, 8298, 1
9290, 9298, 1

```

*NGEN, NSET=NSP2
7298, 7300, 1
8298, 8300, 1
9298, 9300, 1
*NSET, NSET=ALL
NSP1, NSP2
*NODE, NSET=CPSC
300, 30, 0, 0, 0
400, 30, 0, 0, 0
*****
** ELEMENT GENERATION **
*****
*ELEMENT, TYPE=COPE6H, ELSET=MAT
1, 1000, 1025, 9025, 9000, 300, 400
2001, 1200, 1210, 5210, 9200, 300, 400
2101, 1210, 1220, 5220, 5210, 300, 400
2102, 5210, 5220, 9220, 9200, 300, 400
2201, 1220, 1230, 5230, 5220, 300, 400
2501, 1250, 3255, 5255, 5250, 300, 400
2502, 5250, 5255, 7255, 9250, 300, 400
2551, 1250, 1260, 3260, 3255, 300, 400
2552, 3255, 3260, 5260, 5255, 300, 400
2553, 5255, 5260, 7260, 7255, 300, 400
2554, 7255, 7260, 9260, 9250, 300, 400
2601, 1260, 1265, 3265, 3260, 300, 400
2701, 1270, 2272, 3272, 3270, 300, 400
2702, 3270, 3272, 4272, 5270, 300, 400
2703, 5270, 6272, 7272, 7270, 300, 400
2704, 7270, 7272, 8272, 9270, 300, 400
2721, 1270, 1274, 2274, 2272, 300, 400
2722, 2272, 2274, 3274, 3272, 300, 400
2723, 3272, 3274, 4274, 4272, 300, 400
2724, 4272, 4274, 5274, 5270, 300, 400
2725, 5270, 5274, 6274, 6272, 300, 400
2726, 6272, 6274, 7274, 7272, 300, 400
2727, 7272, 7274, 8274, 8272, 300, 400
2728, 8272, 8274, 9274, 9270, 300, 400
2741, 1274, 1276, 2276, 2274, 300, 400
2901, 1290, 1291, 2291, 2290, 300, 400
*ELGEN, ELSET=MAT
1, 8, 25, 250
2201, 3, 10, 100, 2, 4000, 1
2601, 2, 5, 50, 4, 2000, 1
2741, 8, 2, 20, 8, 1000, 1
2901, 10, 1, 10, 8, 1000, 1
*****
** DEFINE ELEMENT SET AND NODE SET **
*****
*ELSET, ELSET=PASS2
2987, 2997, 2988, 2998
*ELSET, ELSET=PLOT1, GENERATE
2741, 2881, 20
2742, 2882, 20
2743, 2883, 20
2744, 2884, 20
2745, 2885, 20

```

2746, 2886, 20
2747, 2887, 20
2748, 2888, 20
2901, 2991, 10
2902, 2992, 10
2903, 2993, 10
2904, 2994, 10
2905, 2995, 10
2906, 2996, 10
2907, 2977, 10
2908, 2978, 10
*ELSET, ELSET=PLOT2
PLOT1
PASS2
*NSET, NSET=CENTER, GENERATE
1300, 9300, 1000

** PLATE THICKNESS (cm) **

*ATTRIBUTE
1. 00

** MATERIAL PROPERTIES OF 316L STAINLESS STEEL PLATE **

*MATERIAL
*ELASTIC
2. 00E+04, 0. 255, 0. 0
2. 00E+07, 0. 255, 10. 0
2. 00E+07, 0. 255, 20. 0
1. 90E+07, 0. 260, 149. 0
1. 81E+07, 0. 290, 260. 0
1. 71E+07, 0. 340, 371. 0
1. 62E+07, 0. 300, 482. 0
1. 53E+07, 0. 320, 593. 0
1. 43E+07, 0. 310, 704. 0
1. 32E+07, 0. 240, 816. 0
2. 00E+06, 0. 240, 1375. 0
*PLASTIC, HARD=KIN
1. 000E+00, 0. 00, 0. 0
1. 000E+00, 0. 10, 0. 0
2. 330E+04, 0. 00, 10. 0
5. 130E+04, 0. 10, 10. 0
2. 330E+04, 0. 00, 20. 0
5. 130E+04, 0. 10, 20. 0
1. 765E+04, 0. 00, 149. 0
4. 425E+04, 0. 10, 149. 0
1. 489E+04, 0. 00, 260. 0
4. 023E+04, 0. 10, 260. 0
1. 345E+04, 0. 00, 371. 0
3. 753E+04, 0. 10, 371. 0
1. 234E+04, 0. 00, 482. 0
3. 502E+04, 0. 10, 482. 0
1. 089E+04, 0. 00, 593. 0
3. 231E+04, 0. 10, 593. 0
0. 920E+04, 0. 00, 704. 0
2. 922E+04, 0. 10, 704. 0

```

0.750E+04,0.00,816.0
2.589E+04,0.10,816.0
1.000E+02,0.00,1375.0
1.000E+02,0.10,1375.0
*EXPANSION,ZERO=20.0
0.000E-00,0.0
0.000E-00,19.9
0.150E-04,20.0
0.168E-04,77.0
0.172E-04,127.0
0.179E-04,327.0
0.184E-04,527.0
0.191E-04,727.0
0.208E-04,1127.0
0.221E-04,1400.0
*****
** APPLY BOUNDARY CONDITIONS **
*****
*BOUNDARY
CENTER,1,1,0.0
1000,2,2,0.0
1025,2,2,0.0
400,1,2,0.0
*****
** DEFINE INITIAL TEMPERATURES **
*****
*INITIAL CONDITIONS, TYPE=TEMPERATURE
ALL,20.0
**
**
*****
** HISTORY DEFINITION
*****
**
*RESTART,WRITE,FREQ=20
*****
** P A S S - 1 **
*****
**
*STEP,AMP=RAMP,INC=1,CYC=10
*STATIC,PTOL=1.0
0.00001,0.00001
*MODEL CHANGE,REMOVE
PASS2
*END STEP
**
*STEP,AMP=RAMP,INC=100,CYC=10
*STATIC,PTOL=300.0
0.004,6000.0,0.00001,1000.0
*TEMPERATURE,FILE=18,BSTEP=2( INC=1),ESTEP=4( INC=10)
NSP1
*EL PRINT
1,1,1,1,1,1
1,1,1,1,1
1,1,1,1,1,1,1,1,1
*NODE PRINT

```

1, 1, 1, 1, 1, 1, 1, 1

*END STEP

**

*STEP, AMP=RAMP, INC=10, CYC=10

*STATIC, PTOL=10. 0

0. 00005, 0. 0001

*TEMPERATURE

NSP1, 20. 0

*END STEP

F. 3. 2 Pass 2

*RESTART, WRITE, READ, ENDSTEP, STEP=3, INC=2, FREQUENCY=10

** P A S S - 2 **

*** INCLUDE 2nd PASS ELEMENTS ***

*STEP, AMP=RAMP, INC=5, CYC=10

*STATIC, PTOL=1. 0

0. 000005, 0. 00001, 0. 000001, 0. 00005

*MODEL CHANGE, INCLUDE

PASS2

*BOUNDARY, OP=MOD

300, 1, 1, -1. 2722E-04

7298, 1, 1, 1. 5318E-02

7298, 2, 2, -8. 2126E-02

7299, 1, 1, -6. 1529E-03

7299, 2, 2, -1. 6583E-02

7300, 1, 1, 0. 00

7300, 2, 2, -6. 1502E-03

8300, 1, 1, 0. 00

9300, 1, 1, 0. 00

8298, 1, 1, 1. 5753E-02

8298, 2, 2, -8. 3483E-02

9298, 1, 1, 1. 7844E-02

9298, 2, 2, -8. 2815E-02

*TEMPERATURE

NSP1, 20. 0

NSP2, 0. 0

*END STEP

**

*STEP, AMP=RAMP, INC=5, CYC=10

*STATIC, PTOL=1. 0

0. 00001, 0. 00001

*BOUNDARY, OP=NEW

1000, 2, 2, 0. 0

1025, 2, 2, 0. 0

CENTER, 1, 1, 0. 0

400, 1, 2, 0. 0

*TEMPERATURE

NSP1, 20. 0

NSP2, 0. 0

*END STEP

**

*STEP, AMP=RAMP, INC=10, CYC=10

*STATIC, PTOL=1. 0

0. 00001, 0. 00001

*TEMPERATURE

NSP1, 20. 0

NSP2, 20. 0

*EL PRINT

1, 1, 1, 1, 1, 1

1, 1, 1, 1, 1

1, 1, 1, 1, 1, 1, 1, 1

*NODE PRINT

1, 1, 1, 1, 1, 1, 1, 1

*END STEP

**

*STEP, AMP=RAMP, INC=100, CYC=10

*STATIC, PTOL=300. 0

0. 004, 6000. 0, 0. 00001, 1000. 0

*TEMPERATURE, FILE=18, BSTEP=6(INC=1), ESTEP=8(INC=10)

NSP1

NSP2

*EL PRINT

1, 1, 1, 1, 1, 1

1, 1, 1, 1, 1

1, 1, 1, 1, 1, 1, 1, 1

*NODE PRINT

1, 1, 1, 1, 1, 1, 1, 1

*END STEP

**

*STEP, AMP=RAMP, INC=10, CYC=10

*STATIC, PTOL=1. 0

0. 000005, 0. 00001

*TEMPERATURE

NSP1, 20. 0

NSP2, 20. 0

*EL PRINT

1, 1, 1, 1, 1, 1

1, 1, 1, 1, 1

1, 1, 1, 1, 1, 1, 1, 1

*NODE PRINT

1, 1, 1, 1, 1, 1, 1, 1

*END STEP

APPENDIX G

ABAQUS INPUT DATA

MULTI-PASS WELD ANALYSES:

TWO PASSES LAID SIDE BY SIDE

G.1 HEAT TRANSFER ANALYSIS INPUT DATA

*HEADING

```
*****
**  TEMPERATURE ANALYSIS FOR 316L STAINLESS STEEL PLATE  **
**  MULTI-PASS WELD - TWO PASSES LAID SIDE BY SIDE      **
*****
**
```

**

```
*****
**  MODEL DEFINITION
```

```
*****
**
```

```
*****
**  NODE GENERATION **
*****
```

```
*NODE, NSET=ALL
1000, 0. 0. 0. 00
9000, 0. 0. 1. 32
1200, 20. 0. 0. 00
9200, 20. 0. 1. 32
1210, 21. 0. 0. 00
5210, 21. 0. 0. 66
1220, 22. 0. 0. 00
5220, 22. 0. 0. 66
9220, 22. 0. 1. 32
1250, 25. 0. 0. 00
5250, 25. 0. 0. 66
9250, 25. 0. 1. 32
3255, 25. 5. 0. 33
5255, 25. 5. 0. 66
7255, 25. 5. 0. 99
1260, 26. 0. 0. 00
3260, 26. 0. 0. 33
5260, 26. 0. 0. 66
7260, 26. 0. 0. 99
9260, 26. 0. 1. 32
1270, 27. 0. 0. 00
3270, 27. 0. 0. 33
5270, 27. 0. 0. 66
7270, 27. 0. 0. 99
9270, 27. 0. 1. 32
2272, 27. 2. 0. 165
3272, 27. 2. 0. 33
4272, 27. 2. 0. 495
6272, 27. 2. 0. 825
7272, 27. 2. 0. 99
8272, 27. 2. 1. 155
1274, 27. 4. 0. 00
2274, 27. 4. 0. 165
3274, 27. 4. 0. 33
4274, 27. 4. 0. 495
5274, 27. 4. 0. 66
6274, 27. 4. 0. 825
7274, 27. 4. 0. 99
```

8274, 27. 4, 1. 155
9274, 27. 4, 1. 32
1326, 32. 6, 0. 00
2326, 32. 6, 0. 165
3326, 32. 6, 0. 33
4326, 32. 6, 0. 495
5326, 32. 6, 0. 66
6326, 32. 6, 0. 825
7326, 32. 6, 0. 99
8326, 32. 6, 1. 155
9326, 32. 6, 1. 32
2328, 32. 8, 0. 165
3328, 32. 8, 0. 33
4328, 32. 8, 0. 495
6328, 32. 8, 0. 825
7328, 32. 8, 0. 99
8328, 32. 8, 1. 155
1330, 33. 0, 0. 00
3330, 33. 0, 0. 33
5330, 33. 0, 0. 66
7330, 33. 0, 0. 99
9330, 33. 0, 1. 32
1340, 34. 0, 0. 00
3340, 34. 0, 0. 33
5340, 34. 0, 0. 66
7340, 34. 0, 0. 99
9340, 34. 0, 1. 32
3345, 34. 5, 0. 33
5345, 34. 5, 0. 66
7345, 34. 5, 0. 99
1350, 35. 0, 0. 0
5350, 35. 0, 0. 66
9350, 35. 0, 1. 32
1380, 38. 0, 0. 0
5380, 38. 0, 0. 66
9380, 38. 0, 1. 32
1390, 39. 0, 0. 0
5390, 39. 0, 0. 66
1400, 40. 0, 0. 0
9400, 40. 0, 1. 32
1600, 60. 0, 0. 0
9600, 60. 0, 1. 32
*NGEN, NSET=ALL
1000, 1200, 25
9000, 9200, 25
1220, 1250, 10
5220, 5250, 10
9220, 9250, 10
1260, 1270, 5
3260, 3270, 5
5260, 5270, 5
7260, 7270, 5
9260, 9270, 5
1274, 1326, 2
2274, 2326, 2
3274, 3326, 2

4274, 4326, 2
5274, 5326, 2
6274, 6326, 2
7274, 7326, 2
8274, 8326, 2
9274, 9326, 2
1330, 1340, 5
3330, 3340, 5
5330, 5340, 5
7330, 7340, 5
9330, 9340, 5
1350, 1380, 10
5350, 5380, 10
9350, 9380, 10
1400, 1600, 25
9400, 9600, 25
*NODE, NSET=ALL
6300, 30, 1, 0, 825
9300, 29, 8, 1, 30
*NGEN, NSET=ALL
6300, 9300, 1000
6294, 6300, 2
7294, 7300, 2
8294, 8300, 2
9294, 9300, 2
6300, 6306, 2
7300, 7306, 2
8300, 8306, 2
9300, 9306, 2

** ELEMENT GENERATION **

*ELEMENT, TYPE=DC2D4, ELSET=MAT
1, 1000, 1025, 9025, 9000
2001, 1200, 1210, 5210, 9200
2101, 1210, 1220, 5220, 5210
2102, 5210, 5220, 9220, 9200
2201, 1220, 1230, 5230, 5220
2501, 1250, 3255, 5255, 5250
2502, 5250, 5255, 7255, 9250
2551, 1250, 1260, 3260, 3255
2552, 3255, 3260, 5260, 5255
2553, 5255, 5260, 7260, 7255
2554, 7255, 7260, 9260, 9250
2601, 1260, 1265, 3265, 3260
2701, 1270, 2272, 3272, 3270
2702, 3270, 3272, 4272, 5270
2703, 5270, 6272, 7272, 7270
2704, 7270, 7272, 8272, 9270
2721, 1270, 1274, 2274, 2272
2722, 2272, 2274, 3274, 3272
2723, 3272, 3274, 4274, 4272
2724, 4272, 4274, 5274, 5270
2725, 5270, 5274, 6274, 6272
2726, 6272, 6274, 7274, 7272
2727, 7272, 7274, 8274, 8272

2728, 8272, 8274, 9274, 9270
 2741, 1274, 1276, 2276, 2274
 3261, 1326, 1330, 2328, 2326
 3262, 2326, 2328, 3328, 3326
 3263, 3326, 3328, 4328, 4326
 3264, 4326, 4328, 5330, 5326
 3265, 5326, 5330, 6328, 6326
 3266, 6326, 6328, 7328, 7326
 3267, 7326, 7328, 8328, 8326
 3268, 8326, 8328, 9330, 9326
 3281, 2328, 1330, 3330, 3328
 3282, 3328, 3330, 5330, 4328
 3283, 6328, 5330, 7330, 7328
 3284, 7328, 7330, 9330, 8328
 3301, 1330, 1335, 3335, 3330
 3401, 1340, 1350, 3345, 3340
 3402, 3340, 3345, 5345, 5340
 3403, 5340, 5345, 7345, 7340
 3404, 7340, 7345, 9350, 9340
 3451, 3345, 1350, 5350, 5345
 3452, 5345, 5350, 9350, 7345
 3501, 1350, 1360, 5360, 5350
 3801, 1380, 1390, 5390, 5380
 3802, 5380, 5390, 9400, 9380
 3901, 1390, 1400, 9400, 5390
 4001, 1400, 1425, 9425, 9400

*ELGEN, ELSET=MAT

1, 8, 25, 250

2201, 3, 10, 100, 2, 4000, 1

2601, 2, 5, 50, 4, 2000, 1

2741, 26, 2, 20, 8, 1000, 1

3301, 2, 5, 50, 4, 2000, 1

3501, 3, 10, 100, 2, 4000, 1

4001, 8, 25, 250

** DEFINE ELEMENT SET AND NODE SET **

*ELSET, ELSET=TOP, GENERATE

1, 1751, 250

2102, 2402, 100

2554, 2654, 50

3304, 3404, 50

3502, 3802, 100

4001, 5751, 250

*ELSET, ELSET=BOT, GENERATE

1, 1751, 250

2001, 2401, 100

2551, 2651, 50

2721, 3261, 20

3301, 3401, 50

3501, 3901, 100

4001, 5751, 250

*ELSET, ELSET=PLOT, GENERATE

2741, 3241, 20

2742, 3242, 20

2743, 3243, 20

```

2744, 3244, 20
2745, 3245, 20
2746, 3246, 20
2747, 3247, 20
2748, 3248, 20
*ELSET, ELSET=HEAT1, GENERATE
2903, 3083, 20
2904, 3084, 20
2905, 3085, 20
2906, 2986, 20
2907, 2987, 20
2908, 2988, 20
3066, 3086, 20
3067, 3087, 20
3068, 3088, 20
*ELSET, ELSET=PASS2, GENERATE
3006, 3046, 20
3007, 3047, 20
3008, 3048, 20
*****
** PLATE THICKNESS (cm) **
*****
*ATTRIBUTE
1.00
*****
** MATERIAL PROPERTIES OF 316L STAINLESS STEEL PLATE **
*****
*MATERIAL, ELSET=MAT
*CONDUCTIVITY
** W/cm K
0.1274, -23.0
0.3136, 1375.0
1.568, 1400.0
*SPECIFIC HEAT
** W/sec g K
0.435, -23.0
0.456, 27.0
0.489, 127.0
0.519, 227.0
0.544, 327.0
0.561, 427.0
0.619, 827.0
0.732, 1375.0
*DENSITY
** g/cm3
7.952
*LATENT HEAT
** J/g
292.0, 1375.0, 1400.0
*****
** DEFINE INITIAL TEMPERATURES **
*****
*INITIAL CONDITIONS, TYPE=TEMPERATURE
ALL, 20.0
**
**

```

```

*****
**  HISTORY DEFINITION
*****
**
*RESTART, WRITE, FREQ=10
*****
**  P A S S  -  1  **
*****
*STEP, AMP=STEP
*HEAT TRANSFER, TEMPTOL=5. 00, END=PERIOD, DELTMX=500. 0
0. 0001, 0. 0001
*MODEL CHANGE, REMOVE
PASS2
*END STEP
**
*STEP, AMP=RAMP, INC=100
*HEAT TRANSFER, TEMPTOL=5. 00, END=PERIOD, DELTMX=500. 0
0. 150, 2. 700
*DFLUX, OP=NEW
HEAT1, BFNU
*FILM
TOP, F3NU
BOT, F1NU
*RADIATE, ZERO=-273. 16
TOP, R3, 20. 0, 3. 12E-12
BOT, R1, 20. 0, 3. 12E-12
*NODE PRINT, NSET=ALL, FREQ=100
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=50
*HEAT TRANSFER, TEMPTOL=2. 00, END=PERIOD, DELTMX=200. 0
0. 50, 297. 2999
*DFLUX, OP=NEW
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=50
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=50
*HEAT TRANSFER, TEMPTOL=1. 00, END=PERIOD, DELTMX=50. 0
50. 0, 5700. 0, 25. 0, 1000. 0
*DFLUX, OP=NEW
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=50
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*****
**  P A S S  -  2  **
*****

```

```
*STEP, AMP=STEP
*HEAT TRANSFER, TEMPTOL=5. 00, END=PERIOD, DELTMX=500. 0
0. 0001, 0. 0001
*MODEL CHANGE, INCLUDE
PASS2
*END STEP
**
*STEP, AMP=RAMP, INC=100
*HEAT TRANSFER, TEMPTOL=5. 00, END=PERIOD, DELTMX=500. 0
0. 150, 2. 700
*DFLUX, OP=NEW
HEAT1, BFNU
PASS2, BFNU
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=100
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=50
*HEAT TRANSFER, TEMPTOL=2. 00, END=PERIOD, DELTMX=200. 0
0. 50, 297. 2999
*DFLUX, OP=NEW
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=50
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=50
*HEAT TRANSFER, TEMPTOL=1. 00, END=PERIOD, DELTMX=50. 0
50. 0, 5700. 0, 25. 0, 1000. 0
*DFLUX, OP=NEW
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=50
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
```


0.2 USER DEFINED SUBROUTINES FOR HEAT TRANSFER ANALYSIS

0.2.1 Subroutine DFLUX

```

CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C   SUBROUTINE TO CALCULATE POWER DENSITY DISTRIBUTION          C
C   FOR TWO PASS WELD - TWO PASS LAID SIDE BY SIDE              C
C   (THIS SUBROUTINE IS CALLED BY ABAQUS DIRECTLY)                C
CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C   SUBROUTINE DFLUX(FLUX, TEMP, KSTEP, KING, TIME, NOEL,
*                               NPT, COORDS, JLTYP)
C   IMPLICIT DOUBLE PRECISION (A-H, O-Z)
C   DIMENSION COORDS(3)

C
C--WELDING PARAMETERS
C
C   AETA=0.90
C   VOLT=34.0
C   CURR=450.0
C   VEL =88.9/60.0

C
C--FRONT QUADRANT (ZETA. GE. 0.0)
C
C   FF=0.8
C   A1=0.6
C   B1=0.6
C   C1=0.3

C
C--REAR QUADRANT (ZETA. LT. 0.0)
C
C   FR=0.2
C   A2=0.6
C   B2=0.6
C   C2=3.7

C
C--CENTER LOCATION AND ORIENTATION OF GOLDAK'S MODEL IN MESH
C
C   IF(TIME.LE.6000.0) GO TO 991
C   IF(TIME.LE.12000.0) GO TO 992
991  XC=29.7
C   YC=1.19
C   ANG=20.0/180.0*3.141592654
C   TI=TIME
C   GO TO 999
992  XC=30.26
C   YC=1.24
C   ANG=0.0
C   TI=TIME-6000.0

C
C--HEAT SOURCE MODEL COORDINATES (X,Y,ZETA)
C
999  XX=COORDS(1)-XC
C   YY=COORDS(2)-YC

C
C--ROTATION OF AXIS (CLOCKWISE POSITIVE)
C

```



```

X=XX*COS(ANG)-YY*SIN(ANG)
Y=XX*SIN(ANG)+YY*COS(ANG)
ZETA=C1-VEL*TI

```

```

C
C--CALCULATE POWER DENSITY
C

```

```

      Q=AETA*VOLT*CURRE
      IF(ZETA.LT.0.0) GO TO 20
      CALL HEAT(FF,A1,B1,C1,Q,X,Y,ZETA,FLUX)
      GO TO 21
20    CALL HEAT(FR,A2,B2,C2,Q,X,Y,ZETA,FLUX)
21    RETURN
      END

```

G.2.2 Subroutine HEAT

```

CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C      SUBROUTINE TO CALCULATE HEAT DISTRIBUTION                      C
C      USING GOLDAK'S ELLIPSOIDAL MODEL                              C
C      (THIS SUBROUTINE IS CALLED BY DFLUX)                            C
CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
      SUBROUTINE HEAT(F,A,B,C,Q,X,Y,ZETA,FLUX)
      IMPLICIT DOUBLE PRECISION (A-H,O-Z)
      PIE=3.1415927
      PP=-3.0*( (X/A)**2 + (Y/B)**2 + (ZETA/C)**2 )
      FLUX=12.0*SQRT(3.0)*F*Q*EXP(PP)/(A*B*C*PIE*SQRT(PIE))
      IF(PP.LT.-3.0) FLUX=0.0
      RETURN
      END

```

G.2.3 Subroutine FILM

```

CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C      SUBROUTINE TO CALCULATE HEAT TRANSFER COEFFICIENTS          C
C      (THIS SUBROUTINE IS CALLED BY ABAQUS DIRECTLY)                C
CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
      SUBROUTINE FILM(H,SINK,TEMP,KSTEP,KINC,TIME,NOEL,
      *NPT,COORDS, JLTP)
      IMPLICIT DOUBLE PRECISION (A-H,O-Z)
      DIMENSION COORDS(3)
      SINK=20.0
C
C--TOP SURFACE
C
      H=(10.0+14.0/175.0*(TEMP-100.0))/10000.0
      IF (TEMP.GT.275.0) H=0.0024
      IF (TEMP.LT.100.0) H=0.0010
C
C--BOTTOM SURFACE
C
      IF(COORDS(2).EQ.0.0) H=H*0.75
      RETURN
      END

```

G.3 RESIDUAL STRESS ANALYSIS INPUT DATA

G.3.1 Pass 1

*HEADING

```
*****
**  RESIDUAL STRESS ANALYSIS FOR 316L STAINLESS STEEL PLATE *
**  MULTI-PASS WELD - TWO PASSES LAID SIDE BY SIDE          *
*****
```

```
**
**  GENERALIZED PLANE STRAIN CONDITION FOR PLATE SECTION
**  PLATE CLAMPED ALONG BOTH EDGES
**
```

```
*****
**  MODEL DEFINITION
*****
```

```
**
*****
**  NODE GENERATION **
*****
```

*NODE

```
1000, 0. 0, 0. 00
9000, 0. 0, 1. 32
1200, 20. 0, 0. 00
9200, 20. 0, 1. 32
1210, 21. 0, 0. 00
5210, 21. 0, 0. 66
1220, 22. 0, 0. 00
5220, 22. 0, 0. 66
9220, 22. 0, 1. 32
1250, 25. 0, 0. 00
5250, 25. 0, 0. 66
9250, 25. 0, 1. 32
3255, 25. 5, 0. 33
5255, 25. 5, 0. 66
7255, 25. 5, 0. 99
1260, 26. 0, 0. 00
3260, 26. 0, 0. 33
5260, 26. 0, 0. 66
7260, 26. 0, 0. 99
9260, 26. 0, 1. 32
1270, 27. 0, 0. 00
3270, 27. 0, 0. 33
5270, 27. 0, 0. 66
7270, 27. 0, 0. 99
9270, 27. 0, 1. 32
2272, 27. 2, 0. 165
3272, 27. 2, 0. 33
4272, 27. 2, 0. 495
6272, 27. 2, 0. 825
7272, 27. 2, 0. 99
8272, 27. 2, 1. 155
1274, 27. 4, 0. 00
2274, 27. 4, 0. 165
3274, 27. 4, 0. 33
4274, 27. 4, 0. 495
```

5274, 27. 4, 0. 66
6274, 27. 4, 0. 825
7274, 27. 4, 0. 99
8274, 27. 4, 1. 155
9274, 27. 4, 1. 32
7300, 30. 0, 0. 99
8300, 30. 0, 1. 155
9300, 30. 0, 1. 32
7306, 30. 6, 0. 99
8306, 30. 6, 1. 155
9306, 30. 6, 1. 32
1326, 32. 6, 0. 00
2326, 32. 6, 0. 165
3326, 32. 6, 0. 33
4326, 32. 6, 0. 495
5326, 32. 6, 0. 66
6326, 32. 6, 0. 825
7326, 32. 6, 0. 99
8326, 32. 6, 1. 155
9326, 32. 6, 1. 32
2328, 32. 8, 0. 165
3328, 32. 8, 0. 33
4328, 32. 8, 0. 495
6328, 32. 8, 0. 825
7328, 32. 8, 0. 99
8328, 32. 8, 1. 155
1330, 33. 0, 0. 00
3330, 33. 0, 0. 33
5330, 33. 0, 0. 66
7330, 33. 0, 0. 99
9330, 33. 0, 1. 32
1340, 34. 0, 0. 00
3340, 34. 0, 0. 33
5340, 34. 0, 0. 66
7340, 34. 0, 0. 99
9340, 34. 0, 1. 32
3345, 34. 5, 0. 33
5345, 34. 5, 0. 66
7345, 34. 5, 0. 99
1350, 35. 0, 0. 0
5350, 35. 0, 0. 66
9350, 35. 0, 1. 32
1380, 38. 0, 0. 0
5380, 38. 0, 0. 66
9380, 38. 0, 1. 32
1390, 39. 0, 0. 0
5390, 39. 0, 0. 66
1400, 40. 0, 0. 0
9400, 40. 0, 1. 32
1600, 60. 0, 0. 0
9600, 60. 0, 1. 32
*NGEN, NSET=NSP1
1000, 1200, 25
9000, 9200, 25
1220, 1250, 10
5220, 5250, 10

9220, 9250, 10
1260, 1270, 5
3260, 3270, 5
5260, 5270, 5
7260, 7270, 5
9260, 9270, 5
1274, 1326, 2
2274, 2326, 2
3274, 3326, 2
4274, 4326, 2
5274, 5326, 2
6274, 6326, 2
7274, 7300, 2
7306, 7326, 2
8274, 8300, 2
8306, 8326, 2
9274, 9300, 2
9306, 9326, 2
1330, 1340, 5
3330, 3340, 5
5330, 5340, 5
7330, 7340, 5
9330, 9340, 5
1350, 1380, 10
5350, 5380, 10
9350, 9380, 10
1400, 1600, 25
9400, 9600, 25
*NODE
6300, 30, 1, 0, 825
9300, 29, 8, 1, 30
*NGEN, NSET=NSP2
6300, 9300, 1000
6300, 6306, 2
7300, 7306, 2
8300, 8306, 2
9300, 9306, 2
*NGEN, NSET=NSP1
6294, 6300, 2
7294, 7300, 2
8294, 8300, 2
9294, 9300, 2
*NSET, NSET=ALL
NSP1, NSP2
*NODE, NSET=CPSC
300, 30, 0, 0, 0
400, 30, 0, 0, 0

** ELEMENT GENERATION **

*ELEMENT, TYPE=COPE6H, ELSET=MAT
1, 1000, 1025, 9025, 9000, 300, 400
2001, 1200, 1210, 5210, 9200, 300, 400
2101, 1210, 1220, 5220, 5210, 300, 400
2102, 5210, 5220, 9220, 9200, 300, 400
2201, 1220, 1230, 5230, 5220, 300, 400

2501, 1250, 3255, 5255, 5250, 300, 400
 2502, 5250, 5255, 7255, 9250, 300, 400
 2551, 1250, 1260, 3260, 3255, 300, 400
 2552, 3255, 3260, 5260, 5255, 300, 400
 2553, 5255, 5260, 7260, 7255, 300, 400
 2554, 7255, 7260, 9260, 9250, 300, 400
 2601, 1260, 1265, 3265, 3260, 300, 400
 2701, 1270, 2272, 3272, 3270, 300, 400
 2702, 3270, 3272, 4272, 5270, 300, 400
 2703, 5270, 6272, 7272, 7270, 300, 400
 2704, 7270, 7272, 8272, 9270, 300, 400
 2721, 1270, 1274, 2274, 2272, 300, 400
 2722, 2272, 2274, 3274, 3272, 300, 400
 2723, 3272, 3274, 4274, 4272, 300, 400
 2724, 4272, 4274, 5274, 5270, 300, 400
 2725, 5270, 5274, 6274, 6272, 300, 400
 2726, 6272, 6274, 7274, 7272, 300, 400
 2727, 7272, 7274, 8274, 8272, 300, 400
 2728, 8272, 8274, 9274, 9270, 300, 400
 2741, 1274, 1276, 2276, 2274, 300, 400
 3261, 1326, 1330, 2328, 2326, 300, 400
 3262, 2326, 2328, 3328, 3326, 300, 400
 3263, 3326, 3328, 4328, 4326, 300, 400
 3264, 4326, 4328, 5330, 5326, 300, 400
 3265, 5326, 5330, 6328, 6326, 300, 400
 3266, 6326, 6328, 7328, 7326, 300, 400
 3267, 7326, 7328, 8328, 8326, 300, 400
 3268, 8326, 8328, 9330, 9326, 300, 400
 3281, 2328, 1330, 3330, 3328, 300, 400
 3282, 3328, 3330, 5330, 4328, 300, 400
 3283, 6328, 5330, 7330, 7328, 300, 400
 3284, 7328, 7330, 9330, 8328, 300, 400
 3301, 1330, 1335, 3335, 3330, 300, 400
 3401, 1340, 1350, 3345, 3340, 300, 400
 3402, 3340, 3345, 5345, 5340, 300, 400
 3403, 5340, 5345, 7345, 7340, 300, 400
 3404, 7340, 7345, 9350, 9340, 300, 400
 3451, 3345, 1350, 5350, 5345, 300, 400
 3452, 5345, 5350, 9350, 7345, 300, 400
 3501, 1350, 1360, 5360, 5350, 300, 400
 3801, 1380, 1390, 5390, 5380, 300, 400
 3802, 5380, 5390, 9400, 9380, 300, 400
 3901, 1390, 1400, 9400, 5390, 300, 400
 4001, 1400, 1425, 9425, 9400, 300, 400

*ELGEN, ELSET=MAT

1, 8, 25, 250

2201, 3, 10, 100, 2, 4000, 1

2601, 2, 5, 50, 4, 2000, 1

2741, 26, 2, 20, 8, 1000, 1

3301, 2, 5, 50, 4, 2000, 1

3501, 3, 10, 100, 2, 4000, 1

4001, 8, 25, 250

** DEFINE ELEMENT SET AND NODE SET **

*ELSET, ELSET=PLOT, GENERATE

```

2741, 3241, 20
2742, 3242, 20
2743, 3243, 20
2744, 3244, 20
2745, 3245, 20
2746, 3246, 20
2747, 3247, 20
2748, 3248, 20
*ELSET, ELSET=PASS2, GENERATE
3006, 3046, 20
3007, 3047, 20
3008, 3048, 20
*****
** PLATE THICKNESS (cm) **
*****
*ATTRIBUTE
1. 00
*****
** MATERIAL PROPERTIES OF 316L STAINLESS STEEL PLATE **
*****
*MATERIAL
*ELASTIC
2. 00E+04, 0. 255, 0. 0
2. 00E+07, 0. 255, 10. 0
2. 00E+07, 0. 255, 20. 0
1. 90E+07, 0. 260, 149. 0
1. 81E+07, 0. 290, 260. 0
1. 71E+07, 0. 340, 371. 0
1. 62E+07, 0. 300, 482. 0
1. 53E+07, 0. 320, 593. 0
1. 43E+07, 0. 310, 704. 0
1. 32E+07, 0. 240, 816. 0
2. 00E+06, 0. 240, 1375. 0
*PLASTIC, HARD=KIN
1. 000E+00, 0. 00, 0. 0
1. 000E+00, 0. 10, 0. 0
2. 330E+04, 0. 00, 10. 0
5. 130E+04, 0. 10, 10. 0
2. 330E+04, 0. 00, 20. 0
5. 130E+04, 0. 10, 20. 0
1. 765E+04, 0. 00, 149. 0
4. 425E+04, 0. 10, 149. 0
1. 489E+04, 0. 00, 260. 0
4. 023E+04, 0. 10, 260. 0
1. 345E+04, 0. 00, 371. 0
3. 753E+04, 0. 10, 371. 0
1. 234E+04, 0. 00, 482. 0
3. 502E+04, 0. 10, 482. 0
1. 089E+04, 0. 00, 593. 0
3. 231E+04, 0. 10, 593. 0
0. 920E+04, 0. 00, 704. 0
2. 922E+04, 0. 10, 704. 0
0. 750E+04, 0. 00, 816. 0
2. 589E+04, 0. 10, 816. 0
1. 000E+02, 0. 00, 1375. 0
1. 000E+02, 0. 10, 1375. 0

```

```

*EXPANSION, ZERO=20. 0
0. 000E-00, 0. 0
0. 000E-00, 19. 9
0. 150E-04, 20. 0
0. 168E-04, 77. 0
0. 172E-04, 127. 0
0. 179E-04, 327. 0
0. 184E-04, 527. 0
0. 191E-04, 727. 0
0. 208E-04, 1127. 0
0. 221E-04, 1400. 0
*****
** APPLY BOUNDARY CONDITIONS **
*****
*BOUNDARY
1000, 1, 2, 0. 0
1025, 2, 2, 0. 0
1575, 2, 2, 0. 0
1600, 2, 2, 0. 0
400, 1, 2, 0. 0
*****
** DEFINE INITIAL TEMPERATURES **
*****
*INITIAL CONDITIONS, TYPE=TEMPERATURE
ALL, 20. 0
**
**
*****
** HISTORY DEFINITION
*****
**
*RESTART, WRITE, FREQ=50
*****
** P A S S - 1 **
*****
**
*STEP, AMP=RAMP, INC=1, CYC=10
*STATIC, PTOL=1. 0
0. 00001, 0. 00001
*MODEL CHANGE, REMOVE
PASS2
*END STEP
**
*STEP, AMP=RAMP, INC=100, CYC=10
*STATIC, PTOL=300. 0
0. 004, 6000. 0, 0. 00001, 1000. 0
*TEMPERATURE, FILE=18, BSTEP=2( INC=1 ), ESTEP=4( INC=10 )
NSP1
*EL PRINT
1, 1, 1, 1, 1, 1
1, 1, 1, 1, 1
1, 1, 1, 1, 1, 1, 1, 1, 1
*NODE PRINT
1, 1, 1, 1, 1, 1, 1, 1, 1
*END STEP
**

```


*STEP, AMP=RAMP, INC=10, CYC=10
*STATIC, PTOL=10.0
0.00005, 0.0001
*TEMPERATURE
NSP1, 20.0
*END STEP

G.3.2 Pass 2

*RESTART, WRITE, READ, ENDSTEP, STEP=3, INC=2, FREQUENCY=10

** P A S S - 2 **

*** INCLUDE 2nd PASS ELEMENTS ***

*STEP, AMP=RAMP, INC=5, CYC=10

*STATIC, PTOL=1.0

0.000005, 0.00001, 0.000001, 0.00005

*MODEL CHANGE, INCLUDE

3006, 3007, 3008, 3026, 3027, 3028, 3046, 3047, 3048

*BOUNDARY, OP=MOD

300, 1, 1, -1.5762E-04

9300, 1, 1, 6.8967E-03

9300, 2, 2, -3.9147E-02

8300, 1, 1, 9.2775E-03

8300, 2, 2, -4.2231E-02

7300, 1, 1, -1.3170E-03

7300, 2, 2, -5.0553E-02

6300, 1, 1, -1.9226E-02

6300, 2, 2, -5.3979E-02

6302, 1, 1, -1.5734E-02

6302, 2, 2, -5.1430E-02

6304, 1, 1, -1.4757E-02

6304, 2, 2, -5.1001E-02

6306, 1, 1, -1.3619E-02

6306, 2, 2, -4.9272E-02

7306, 1, 1, -1.3549E-02

7306, 2, 2, -4.9220E-02

8306, 1, 1, -1.4413E-02

8306, 2, 2, -4.9124E-02

9306, 1, 1, -1.5180E-02

9306, 2, 2, -4.9088E-02

*TEMPERATURE

NSP1, 20.0

NSP2, 0.0

*END STEP

*STEP, AMP=RAMP, INC=5, CYC=10

*STATIC, PTOL=1.0

0.00001, 0.00001

*BOUNDARY, OP=NEW

1000, 1, 2, 0.0

1025, 2, 2, 0.0

1575, 2, 2, 0.0

1600, 2, 2, 0.0

400, 1, 2, 0.0

*TEMPERATURE

NSP1, 20.0

NSP2, 0.0

*END STEP

*STEP, AMP=RAMP, INC=10, CYC=10

*STATIC, PTOL=1.0

0.00001, 0.00001

```
*TEMPERATURE
NSP1, 20. 0
NSP2, 20. 0
*EL PRINT
1, 1, 1, 1, 1, 1
1, 1, 1, 1, 1
1, 1, 1, 1, 1, 1, 1, 1, 1
*NODE PRINT
1, 1, 1, 1, 1, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=100, CYC=10
*STATIC, PTOL=300. 0
0. 004, 6000. 0, 0. 00001, 1000. 0
*TEMPERATURE, FILE=18, BSTEP=6( INC=1), ESTEP=8( INC=10)
NSP1
NSP2
*EL PRINT
1, 1, 1, 1, 1, 1
1, 1, 1, 1, 1
1, 1, 1, 1, 1, 1, 1, 1, 1
*NODE PRINT
1, 1, 1, 1, 1, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=10, CYC=10
*STATIC, PTOL=1. 0
0. 000005, 0. 00001
*TEMPERATURE
NSP1, 20. 0
NSP2, 20. 0
*EL PRINT
1, 1, 1, 1, 1, 1
1, 1, 1, 1, 1
1, 1, 1, 1, 1, 1, 1, 1, 1
*NODE PRINT
1, 1, 1, 1, 1, 1, 1, 1, 1
*END STEP
```

APPENDIX H

ABAQUS INPUT DATA

MULTI-PASS WELD ANALYSES:

THREE PASSES LAID SIDE BY SIDE

H.1 HEAT TRANSFER ANALYSIS INPUT DATA

*HEADING

```
*****  
**  TEMPERATURE ANALYSIS FOR 316L STAINLESS STEEL PLATE  **  
**  MULTI-PASS WELD - THRESS PASSES LAID SIDE BY SIDE  **  
*****  
**
```

```
*****  
**  MODEL DEFINITION  
*****
```

```
**
```

```
*****  
**  NODE GENERATION **
```

```
*****
```

```
*NODE, NSET=ALL
```

```
1000, 0. 0, 0. 00
```

```
9000, 0. 0, 1. 32
```

```
1200, 20. 0, 0. 00
```

```
9200, 20. 0, 1. 32
```

```
1210, 21. 0, 0. 00
```

```
5210, 21. 0, 0. 66
```

```
1220, 22. 0, 0. 00
```

```
5220, 22. 0, 0. 66
```

```
9220, 22. 0, 1. 32
```

```
1250, 25. 0, 0. 00
```

```
5250, 25. 0, 0. 66
```

```
9250, 25. 0, 1. 32
```

```
3255, 25. 5, 0. 33
```

```
5255, 25. 5, 0. 66
```

```
7255, 25. 5, 0. 99
```

```
1260, 26. 0, 0. 00
```

```
3260, 26. 0, 0. 33
```

```
5260, 26. 0, 0. 66
```

```
7260, 26. 0, 0. 99
```

```
9260, 26. 0, 1. 32
```

```
1270, 27. 0, 0. 00
```

```
3270, 27. 0, 0. 33
```

```
5270, 27. 0, 0. 66
```

```
7270, 27. 0, 0. 99
```

```
9270, 27. 0, 1. 32
```

```
2272, 27. 2, 0. 165
```

```
3272, 27. 2, 0. 33
```

```
4272, 27. 2, 0. 495
```

```
6272, 27. 2, 0. 825
```

```
7272, 27. 2, 0. 99
```

```
8272, 27. 2, 1. 155
```

```
1274, 27. 4, 0. 00
```

```
2274, 27. 4, 0. 165
```

```
3274, 27. 4, 0. 33
```

```
4274, 27. 4, 0. 495
```

```
5274, 27. 4, 0. 66
```

```
6274, 27. 4, 0. 825
```

```
7274, 27. 4, 0. 99
```

```
8274, 27. 4, 1. 155
```

```
9274, 27. 4, 1. 32
```

```
1326, 32. 6, 0. 00
```

2326, 32. 6, 0. 165
3326, 32. 6, 0. 33
4326, 32. 6, 0. 495
5326, 32. 6, 0. 66
6326, 32. 6, 0. 825
7326, 32. 6, 0. 99
8326, 32. 6, 1. 155
9326, 32. 6, 1. 32
2328, 32. 8, 0. 165
3328, 32. 8, 0. 33
4328, 32. 8, 0. 495
6328, 32. 8, 0. 825
7328, 32. 8, 0. 99
8328, 32. 8, 1. 155
1330, 33. 0, 0. 00
3330, 33. 0, 0. 33
5330, 33. 0, 0. 66
7330, 33. 0, 0. 99
9330, 33. 0, 1. 32
1340, 34. 0, 0. 00
3340, 34. 0, 0. 33
5340, 34. 0, 0. 66
7340, 34. 0, 0. 99
9340, 34. 0, 1. 32
3345, 34. 5, 0. 33
5345, 34. 5, 0. 66
7345, 34. 5, 0. 99
1350, 35. 0, 0. 0
5350, 35. 0, 0. 66
9350, 35. 0, 1. 32
1380, 38. 0, 0. 0
5380, 38. 0, 0. 66
9380, 38. 0, 1. 32
1390, 39. 0, 0. 0
5390, 39. 0, 0. 66
1400, 40. 0, 0. 0
9400, 40. 0, 1. 32
1600, 60. 0, 0. 0
9600, 60. 0, 1. 32
*NGEN, NSET=ALL
1000, 1200, 25
9000, 9200, 25
1220, 1250, 10
5220, 5250, 10
9220, 9250, 10
1260, 1270, 5
3260, 3270, 5
5260, 5270, 5
7260, 7270, 5
9260, 9270, 5
1274, 1326, 2
2274, 2326, 2
3274, 3326, 2
4274, 4326, 2
5274, 5326, 2
6274, 6326, 2

7274, 7326, 2
8274, 8326, 2
9274, 9326, 2
1330, 1340, 5
3330, 3340, 5
5330, 5340, 5
7330, 7340, 5
9330, 9340, 5
1350, 1380, 10
5350, 5380, 10
9350, 9380, 10
1400, 1600, 25
9400, 9600, 25
*NODE, NSET=ALL
6298, 29, 85, 0, 825
6304, 30, 50, 0, 825
9298, 29, 5, 1, 32
9304, 30, 2, 1, 32
*NGEN, NSET=ALL
6298, 9298, 1000
6304, 9304, 1000
6290, 6298, 2
6298, 6304, 2
6304, 6310, 2
7290, 7298, 2
7298, 7304, 2
7304, 7310, 2
8290, 8298, 2
8298, 8304, 2
8304, 8310, 2
9290, 9298, 2
9298, 9304, 2
9304, 9310, 2

** ELEMENT GENERATION **

*ELEMENT, TYPE=DC2D4, ELSET=MAT
1, 1000, 1025, 9025, 9000
2001, 1200, 1210, 5210, 9200
2101, 1210, 1220, 5220, 5210
2102, 5210, 5220, 9220, 9200
2201, 1220, 1230, 5230, 5220
2501, 1250, 3255, 5255, 5250
2502, 5250, 5255, 7255, 9250
2551, 1250, 1260, 3260, 3255
2552, 3255, 3260, 5260, 5255
2553, 5255, 5260, 7260, 7255
2554, 7255, 7260, 9260, 9250
2601, 1260, 1265, 3265, 3260
2701, 1270, 2272, 3272, 3270
2702, 3270, 3272, 4272, 5270
2703, 5270, 6272, 7272, 7270
2704, 7270, 7272, 8272, 9270
2721, 1270, 1274, 2274, 2272
2722, 2272, 2274, 3274, 3272
2723, 3272, 3274, 4274, 4272

2724, 4272, 4274, 5274, 5270
 2725, 5270, 5274, 6274, 6272
 2726, 6272, 6274, 7274, 7272
 2727, 7272, 7274, 8274, 8272
 2728, 8272, 8274, 9274, 9270
 2741, 1274, 1276, 2276, 2274
 3261, 1326, 1330, 2328, 2326
 3262, 2326, 2328, 3328, 3326
 3263, 3326, 3328, 4328, 4326
 3264, 4326, 4328, 5330, 5326
 3265, 5326, 5330, 6328, 6326
 3266, 6326, 6328, 7328, 7326
 3267, 7326, 7328, 8328, 8326
 3268, 8326, 8328, 9330, 9326
 3281, 2328, 1330, 3330, 3328
 3282, 3328, 3330, 5330, 4328
 3283, 6328, 5330, 7330, 7328
 3284, 7328, 7330, 9330, 8328
 3301, 1330, 1335, 3335, 3330
 3401, 1340, 1350, 3345, 3340
 3402, 3340, 3345, 5345, 5340
 3403, 5340, 5345, 7345, 7340
 3404, 7340, 7345, 9350, 9340
 3451, 3345, 1350, 5350, 5345
 3452, 5345, 5350, 9350, 7345
 3501, 1350, 1360, 5360, 5350
 3801, 1380, 1390, 5390, 5380
 3802, 5380, 5390, 9400, 9380
 3901, 1390, 1400, 9400, 5390
 4001, 1400, 1425, 9425, 9400

*ELGEN, ELSET=MAT

1, 8, 25, 250

2201, 3, 10, 100, 2, 4000, 1

2601, 2, 5, 50, 4, 2000, 1

2741, 26, 2, 20, 8, 1000, 1

3301, 2, 5, 50, 4, 2000, 1

3501, 3, 10, 100, 2, 4000, 1

4001, 8, 25, 250

** DEFINE ELEMENT SET AND NODE SET **

*ELSET, ELSET=TOP, GENERATE

1, 1751, 250

2102, 2402, 100

2554, 2654, 50

3304, 3404, 50

3502, 3802, 100

4001, 5751, 250

*ELSET, ELSET=BOT, GENERATE

1, 1751, 250

2001, 2401, 100

2551, 2651, 50

2721, 3261, 20

3301, 3401, 50

3501, 3901, 100

4001, 5751, 250

```
*ELSET, ELSET=PLOT, GENERATE
2701, 2704, 1
2721, 2728, 1
2741, 3261, 20
2742, 3262, 20
2743, 3263, 20
2744, 3264, 20
2745, 3265, 20
2746, 3266, 20
2747, 3267, 20
2748, 3268, 20
3261, 3268, 1
3281, 3284, 1
*ELSET, ELSET=HEAT1, GENERATE
2843, 3143, 20
2844, 3144, 20
2845, 3145, 20
2846, 2966, 20
3106, 3146, 20
2847, 2967, 20
3107, 3147, 20
2848, 2968, 20
3108, 3148, 20
*ELSET, ELSET=PASS2, GENERATE
2986, 3026, 20
2987, 3027, 20
2988, 3028, 20
*ELSET, ELSET=PASS3, GENERATE
3046, 3086, 20
3047, 3087, 20
3048, 3088, 20
*****
** PLATE THICKNESS (cm) **
*****
*ATTRIBUTE
1. 00
*****
** MATERIAL PROPERTIES OF 316L STAINLESS STEEL PLATE **
*****
*MATERIAL, ELSET=MAT
*CONDUCTIVITY
** W/cm K
0. 1274, -23. 0
0. 3136, 1375. 0
1. 568, 1400. 0
*SPECIFIC HEAT
** W/sec g K
0. 435, -23. 0
0. 456, 27. 0
0. 489, 127. 0
0. 519, 227. 0
0. 544, 327. 0
0. 561, 427. 0
0. 619, 827. 0
0. 732, 1375. 0
*DENSITY
```



```

** g/cm3
7.952
*LATENT HEAT
** J/g
292.0, 1375.0, 1400.0
*****
** DEFINE INITIAL TEMPERATURES **
*****
*INITIAL CONDITIONS, TYPE=TEMPERATURE
ALL, 20.0
**
**
*****
** HISTORY DEFINITION
*****
**
*RESTART, WRITE, FREQ=10
*****
** P A S S - 1 **
*****
*STEP, AMP=STEP
*HEAT TRANSFER, TEMPTOL=5.00, END=PERIOD, DELTMX=500.0
0.0001, 0.0001
*MODEL CHANGE, REMOVE
PASS2
PASS3
*END STEP
**
*STEP, AMP=RAMP, INC=100
*HEAT TRANSFER, TEMPTOL=5.00, END=PERIOD, DELTMX=500.0
0.150, 2.700
*DFLUX, OP=NEW
HEAT1, BFNU
*FILM
TOP, F3NU
BOT, F1NU
*RADIATE, ZERO=-273.16
TOP, R3, 20.0, 3.12E-12
BOT, R1, 20.0, 3.12E-12
*NODE PRINT, NSET=ALL, FREQ=100
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=50
*HEAT TRANSFER, TEMPTOL=2.00, END=PERIOD, DELTMX=200.0
0.50, 297.2999
*DFLUX, OP=NEW
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273.16
*NODE PRINT, NSET=ALL, FREQ=50
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=50

```

```

*HEAT TRANSFER, TEMPTOL=1. 00, END=PERIOD, DELTMX=50. 0
50. 0, 5700. 0, 25. 0, 1000. 0
*DFLUX, OP=NEW
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=50
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
**
*****
** P A S S - 2 **
*****
**
*STEP, AMP=STEP
*HEAT TRANSFER, TEMPTOL=5. 00, END=PERIOD, DELTMX=500. 0
0. 0001, 0. 0001
*MODEL CHANGE, INCLUDE
PASS2
*END STEP
**
*STEP, AMP=RAMP, INC=100
*HEAT TRANSFER, TEMPTOL=5. 00, END=PERIOD, DELTMX=500. 0
0. 150, 2. 700
*DFLUX, OP=NEW
HEAT1, BFNU
PASS2, BFNU
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=100
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=50
*HEAT TRANSFER, TEMPTOL=2. 00, END=PERIOD, DELTMX=200. 0
0. 50, 297. 2999
*DFLUX, OP=NEW
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=50
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=50
*HEAT TRANSFER, TEMPTOL=1. 00, END=PERIOD, DELTMX=50. 0
50. 0, 5700. 0, 25. 0, 1000. 0
*DFLUX, OP=NEW
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=50
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP

```

```
**
*****
** P A S S - 3 **
*****
**
*STEP, AMP=STEP
*HEAT TRANSFER, TEMPTOL=5. 00, END=PERIOD, DELTMX=500. 0
0. 0001, 0. 0001
*MODEL CHANGE, INCLUDE
PASS3
*END STEP
**
*STEP, AMP=RAMP, INC=100
*HEAT TRANSFER, TEMPTOL=5. 00, END=PERIOD, DELTMX=500. 0
0. 150, 2. 700
*DFLUX, OP=NEW
HEAT1, BFNU
PASS2, BFNU
PASS3, BFNU
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=100
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=50
*HEAT TRANSFER, TEMPTOL=2. 00, END=PERIOD, DELTMX=200. 0
0. 50, 297. 2999
*DFLUX, OP=NEW
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=50
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=50
*HEAT TRANSFER, TEMPTOL=1. 00, END=PERIOD, DELTMX=50. 0
50. 0, 5700. 0, 25. 0, 1000. 0
*DFLUX, OP=NEW
*FILM, OP=MOD
*RADIATE, OP=MOD, ZERO=-273. 16
*NODE PRINT, NSET=ALL, FREQ=50
*NODE FILE, NSET=ALL
2, 1, 1, 1, 1
**
*END STEP
```

H.2 USER DEFINED SUBROUTINES FOR HEAT TRANSFER ANALYSIS

H.2.1 Subroutine DFLUX

```

CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C   SUBROUTINE TO CALCULATE POWER DENSITY DISTRIBUTION          C
C   FOR THREE PASS WELD - THREE PASSES LAID SIDE BY SIDE      C
C   (THIS SUBROUTINE IS CALLED BY ABAQUS DIRECTLY)              C
CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C
C   SUBROUTINE DFLUX(FLUX, TEMP, KSTEP, KINC, TIME, NOEL,
C   *               NPT, COORDS, JLTYP)
C   IMPLICIT DOUBLE PRECISION (A-H, O-Z)
C   DIMENSION COORDS(3)
C
C--WELDING PARAMETERS
C
C   AETA=0.90
C   VOLT=34.0
C   CURR=450.0
C   VEL =88.9/60.0
C
C--FRONT QUADRANT (ZETA.QE.0.0)
C
C   FF=0.8
C   A1=0.6
C   B1=0.6
C   C1=0.3
C
C--REAR QUADRANT (ZETA.LT.0.0)
C
C   FR=0.2
C   A2=0.6
C   B2=0.6
C   C2=3.7
C
C--CENTER LOCATION AND ORIENTATION OF GOLDAK'S MODEL IN MESH
C
C   IF(TIME.LE.6000.0) GO TO 991
C   IF(TIME.LE.12000.0) GO TO 992
C   IF(TIME.LE.18000.0) GO TO 993
C
991  XC=29.4
C   YC=1.30
C   ANG=25.0/180.0*3.141592654
C   TI=TIME
C   GO TO 999
C
992  XC=30.00
C   YC=1.30
C   ANG=25.0/180.0*3.141592654
C   TI=TIME-6000.0
C   GO TO 999
C
993  XC=30.65
C   YC=1.30
C   ANG=0.0
C   TI=TIME-12000.0

```

```

C
C--HEAT SOURCE MODEL COORDINATES (X,Y,ZETA)

```

```

C
  999  XX=COORDS(1)-XC
       YY=COORDS(2)-YC

```

```

C
C--ROTATION OF AXIS (CLOCKWISE POSITIVE)

```

```

C
  X=XX*COS(ANG)-YY*SIN(ANG)
  Y=XX*SIN(ANG)+YY*COS(ANG)
  ZETA=C1-VEL*TI

```

```

C
C--CALCULATE POWER DENSITY

```

```

C
  Q=AETA*VOLT*CURRE
  IF(ZETA.LT.0.0) GO TO 20
  CALL HEAT(F, A1, B1, C1, Q, X, Y, ZETA, FLUX)
  GO TO 21
20  CALL HEAT(FR, A2, B2, C2, Q, X, Y, ZETA, FLUX)
21  RETURN
  END

```

H. 2.2 Subroutine HEAT

```

CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C  SUBROUTINE TO CALCULATE HEAT DISTRIBUTION                      C
C  USING GOLDAK'S ELLIPSOIDAL MODEL                               C
C  (THIS SUBROUTINE IS CALLED BY DFLUX)                            C
CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C
  SUBROUTINE HEAT(F, A, B, C, Q, X, Y, ZETA, FLUX)
  IMPLICIT DOUBLE PRECISION (A-H, O-Z)

C
  PIE=3.1415927
  PP=-3.0*( (X/A)**2 + (Y/B)**2 + (ZETA/C)**2 )
  FLUX=12.0*SQRT(3.0)*F*Q*EXP(PP)/(A*B*C*PIE*SQRT(PIE))
  IF(PP.LT.-3.0)FLUX=0.0
  RETURN
  END

```

H. 2.3 Subroutine FILM

```

CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C  SUBROUTINE TO CALCULATE HEAT TRANSFER COEFFICIENTS            C
C  (THIS SUBROUTINE IS CALLED BY ABAQUS DIRECTLY)                 C
CCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCCC
C
  SUBROUTINE FILM(H, SINK, TEMP, KSTEP, KINC, TIME, NOEL,
  *NPT, COORDS, JLTP)
  IMPLICIT DOUBLE PRECISION (A-H, O-Z)
  DIMENSION COORDS(3)
  SINK=20.0

```

```

C
C--TOP SURFACE
C

```

```
H=(10.0+14.0/175.0*(TEMP-100.0))/10000.0  
IF (TEMP.GT.275.0) H=0.0024  
IF (TEMP.LT.100.0) H=0.0010
```

```
C  
C--BOTTOM SURFACE  
C
```

```
IF(COORDS(2).EQ.0.0) H=H*0.75  
RETURN  
END
```

H.3 RESIDUAL STRESS ANALYSIS INPUT DATA

H.3.1 Pass 1

*HEADING

```
*****
**  RESIDUAL STRESS ANALYSIS FOR 316L STAINLESS STEEL PLATE *
**  MULTI-PASS WELD - THREE PASSES LAID SIDE BY SIDE      *
*****
```

```
**
**  GENERALIZED PLANE STRAIN CONDITION FOR PLATE SECTION
**  PLATE CLAMPED ALONG BOTH EDGES
**
```

```
*****
**  MODEL DEFINITION
*****
```

```
**
*****
** NODE GENERATION **
*****
```

*NODE

```
1000, 0. 0, 0. 00
9000, 0. 0, 1. 32
1200, 20. 0, 0. 00
9200, 20. 0, 1. 32
1210, 21. 0, 0. 00
5210, 21. 0, 0. 66
1220, 22. 0, 0. 00
5220, 22. 0, 0. 66
9220, 22. 0, 1. 32
1250, 25. 0, 0. 00
5250, 25. 0, 0. 66
9250, 25. 0, 1. 32
3255, 25. 5, 0. 33
5255, 25. 5, 0. 66
7255, 25. 5, 0. 99
1260, 26. 0, 0. 00
3260, 26. 0, 0. 33
5260, 26. 0, 0. 66
7260, 26. 0, 0. 99
9260, 26. 0, 1. 32
1270, 27. 0, 0. 00
3270, 27. 0, 0. 33
5270, 27. 0, 0. 66
7270, 27. 0, 0. 99
9270, 27. 0, 1. 32
2272, 27. 2, 0. 165
3272, 27. 2, 0. 33
4272, 27. 2, 0. 495
6272, 27. 2, 0. 825
7272, 27. 2, 0. 99
8272, 27. 2, 1. 155
1274, 27. 4, 0. 00
2274, 27. 4, 0. 165
3274, 27. 4, 0. 33
4274, 27. 4, 0. 495
```

5274, 27. 4. 0. 66
6274, 27. 4. 0. 825
7274, 27. 4. 0. 99
8274, 27. 4. 1. 155
9274, 27. 4. 1. 32
7298, 29. 8. 0. 99
8298, 29. 8. 1. 155
9298, 29. 8. 1. 32
7310, 31. 0. 0. 99
8310, 31. 0. 1. 155
9310, 31. 0. 1. 32
1326, 32. 6. 0. 00
2326, 32. 6. 0. 165
3326, 32. 6. 0. 33
4326, 32. 6. 0. 495
5326, 32. 6. 0. 66
6326, 32. 6. 0. 825
7326, 32. 6. 0. 99
8326, 32. 6. 1. 155
9326, 32. 6. 1. 32
2328, 32. 8. 0. 165
3328, 32. 8. 0. 33
4328, 32. 8. 0. 495
6328, 32. 8. 0. 825
7328, 32. 8. 0. 99
8328, 32. 8. 1. 155
1330, 33. 0. 0. 00
3330, 33. 0. 0. 33
5330, 33. 0. 0. 66
7330, 33. 0. 0. 99
9330, 33. 0. 1. 32
1340, 34. 0. 0. 00
3340, 34. 0. 0. 33
5340, 34. 0. 0. 66
7340, 34. 0. 0. 99
9340, 34. 0. 1. 32
3345, 34. 5. 0. 33
5345, 34. 5. 0. 66
7345, 34. 5. 0. 99
1350, 35. 0. 0. 0
5350, 35. 0. 0. 66
9350, 35. 0. 1. 32
1380, 38. 0. 0. 0
5380, 38. 0. 0. 66
9380, 38. 0. 1. 32
1390, 39. 0. 0. 0
5390, 39. 0. 0. 66
1400, 40. 0. 0. 0
9400, 40. 0. 1. 32
1600, 60. 0. 0. 0
9600, 60. 0. 1. 32
*NGEN, NSET=NSP1
1000, 1200, 25
9000, 9200, 25
1220, 1250, 10
5220, 5250, 10

9220, 9250, 10
1260, 1270, 5
3260, 3270, 5
5260, 5270, 5
7260, 7270, 5
9260, 9270, 5
1274, 1326, 2
2274, 2326, 2
3274, 3326, 2
4274, 4326, 2
5274, 5326, 2
6274, 6326, 2
7274, 7298, 2
7310, 7326, 2
8274, 8298, 2
8310, 8326, 2
9274, 9298, 2
9310, 9326, 2
1330, 1340, 5
3330, 3340, 5
5330, 5340, 5
7330, 7340, 5
9330, 9340, 5
1350, 1380, 10
5350, 5380, 10
9350, 9380, 10
1400, 1600, 25
9400, 9600, 25
*NODE
6298, 29, 85, 0, 825
6304, 30, 50, 0, 825
9298, 29, 5, 1, 32
9304, 30, 2, 1, 32
*NGEN, NSET=NSP2
6298, 9298, 1000
6304, 9304, 1000
6298, 6304, 2
7298, 7304, 2
8298, 8304, 2
9298, 9304, 2
*NGEN, NSET=NSP3
6304, 6310, 2
7304, 7310, 2
8304, 8310, 2
9304, 9310, 2
*NGEN, NSET=NSP1
6290, 6298, 2
7290, 7298, 2
8290, 8298, 2
9290, 9298, 2
*NSET, NSET=ALL
NSP1, NSP2, NSP3
*NODE, NSET=GPSC
300, 30, 0, 0, 0
400, 30, 0, 0, 0

**** ELEMENT GENERATION ****************ELEMENT, TYPE=CQPE6H, ELSET=MAT**

1, 1000, 1025, 9025, 9000, 300, 400
2001, 1200, 1210, 5210, 9200, 300, 400
2101, 1210, 1220, 5220, 5210, 300, 400
2102, 5210, 5220, 9220, 9200, 300, 400
2201, 1220, 1230, 5230, 5220, 300, 400
2501, 1250, 3255, 5255, 5250, 300, 400
2502, 5250, 5255, 7255, 9250, 300, 400
2551, 1250, 1260, 3260, 3255, 300, 400
2552, 3255, 3260, 5260, 5255, 300, 400
2553, 5255, 5260, 7260, 7255, 300, 400
2554, 7255, 7260, 9260, 9250, 300, 400
2601, 1260, 1265, 3265, 3260, 300, 400
2701, 1270, 2272, 3272, 3270, 300, 400
2702, 3270, 3272, 4272, 5270, 300, 400
2703, 5270, 6272, 7272, 7270, 300, 400
2704, 7270, 7272, 8272, 9270, 300, 400
2721, 1270, 1274, 2274, 2272, 300, 400
2722, 2272, 2274, 3274, 3272, 300, 400
2723, 3272, 3274, 4274, 4272, 300, 400
2724, 4272, 4274, 5274, 5270, 300, 400
2725, 5270, 5274, 6274, 6272, 300, 400
2726, 6272, 6274, 7274, 7272, 300, 400
2727, 7272, 7274, 8274, 8272, 300, 400
2728, 8272, 8274, 9274, 9270, 300, 400
2741, 1274, 1276, 2276, 2274, 300, 400
3261, 1326, 1330, 2328, 2326, 300, 400
3262, 2326, 2328, 3328, 3326, 300, 400
3263, 3326, 3328, 4328, 4326, 300, 400
3264, 4326, 4328, 5330, 5326, 300, 400
3265, 5326, 5330, 6328, 6326, 300, 400
3266, 6326, 6328, 7328, 7326, 300, 400
3267, 7326, 7328, 8328, 8326, 300, 400
3268, 8326, 8328, 9330, 9326, 300, 400
3281, 2328, 1330, 3330, 3328, 300, 400
3282, 3328, 3330, 5330, 4328, 300, 400
3283, 6328, 5330, 7330, 7328, 300, 400
3284, 7328, 7330, 9330, 8328, 300, 400
3301, 1330, 1335, 3335, 3330, 300, 400
3401, 1340, 1350, 3345, 3340, 300, 400
3402, 3340, 3345, 5345, 5340, 300, 400
3403, 5340, 5345, 7345, 7340, 300, 400
3404, 7340, 7345, 9350, 9340, 300, 400
3451, 3345, 1350, 5350, 5345, 300, 400
3452, 5345, 5350, 9350, 7345, 300, 400
3501, 1350, 1360, 5360, 5350, 300, 400
3801, 1380, 1390, 5390, 5380, 300, 400
3802, 5380, 5390, 9400, 9380, 300, 400
3901, 1390, 1400, 9400, 5390, 300, 400
4001, 1400, 1425, 9425, 9400, 300, 400

***ELQEN, ELSET=MAT**

1, 8, 25, 250
2201, 3, 10, 100, 2, 4000, 1
2601, 2, 5, 50, 4, 2000, 1

```

2741, 26, 2, 20, 8, 1000, 1
3301, 2, 5, 50, 4, 2000, 1
3501, 3, 10, 100, 2, 4000, 1
4001, 8, 25, 250
*****
** DEFINE ELEMENT SET AND NODE SET **
*****
*ELSET, ELSET=PLOT, GENERATE
2701, 2704, 1
2721, 2728, 1
2741, 3261, 20
2742, 3262, 20
2743, 3263, 20
2744, 3264, 20
2745, 3265, 20
2746, 3266, 20
2747, 3267, 20
2748, 3268, 20
3261, 3268, 1
3281, 3284, 1
*ELSET, ELSET=PASS2, GENERATE
2986, 3026, 20
2987, 3027, 20
2988, 3028, 20
*ELSET, ELSET=PASS3, GENERATE
3046, 3086, 20
3047, 3087, 20
3048, 3088, 20
*****
** PLATE THICKNESS (cm) **
*****
*ATTRIBUTE
1. 00
*****
** MATERIAL PROPERTIES OF 316L STAINLESS STEEL PLATE **
*****
*MATERIAL
*ELASTIC
2. 00E+04, 0. 255, 0. 0
2. 00E+07, 0. 255, 10. 0
2. 00E+07, 0. 255, 20. 0
1. 90E+07, 0. 260, 149. 0
1. 81E+07, 0. 290, 260. 0
1. 71E+07, 0. 340, 371. 0
1. 62E+07, 0. 300, 482. 0
1. 53E+07, 0. 320, 593. 0
1. 43E+07, 0. 310, 704. 0
1. 32E+07, 0. 240, 816. 0
2. 00E+06, 0. 240, 1375. 0
*PLASTIC, HARD=KIN
1. 000E+00, 0. 00, 0. 0
1. 000E+00, 0. 10, 0. 0
2. 330E+04, 0. 00, 10. 0
5. 130E+04, 0. 10, 10. 0
2. 330E+04, 0. 00, 20. 0
5. 130E+04, 0. 10, 20. 0

```

```

1. 765E+04, 0. 00, 149. 0
4. 425E+04, 0. 10, 149. 0
1. 489E+04, 0. 00, 260. 0
4. 023E+04, 0. 10, 260. 0
1. 345E+04, 0. 00, 371. 0
3. 753E+04, 0. 10, 371. 0
1. 234E+04, 0. 00, 482. 0
3. 502E+04, 0. 10, 482. 0
1. 089E+04, 0. 00, 593. 0
3. 231E+04, 0. 10, 593. 0
0. 920E+04, 0. 00, 704. 0
2. 922E+04, 0. 10, 704. 0
0. 750E+04, 0. 00, 816. 0
2. 589E+04, 0. 10, 816. 0
1. 000E+02, 0. 00, 1375. 0
1. 000E+02, 0. 10, 1375. 0
*EXPANSION, ZERO=20. 0
0. 000E-00, 0. 0
0. 000E-00, 19. 9
0. 150E-04, 20. 0
0. 168E-04, 77. 0
0. 172E-04, 127. 0
0. 179E-04, 327. 0
0. 184E-04, 527. 0
0. 191E-04, 727. 0
0. 208E-04, 1127. 0
0. 221E-04, 1400. 0
*****
** APPLY BOUNDARY CONDITIONS **
*****
*BOUNDARY
1000, 1, 2, 0. 0
1025, 2, 2, 0. 0
1575, 2, 2, 0. 0
1600, 2, 2, 0. 0
400, 1, 2, 0. 0
*****
** DEFINE INITIAL TEMPERATURES **
*****
*INITIAL CONDITIONS, TYPE=TEMPERATURE
ALL, 20. 0
**
**
*****
** HISTORY DEFINITION
*****
**
*RESTART, WRITE, FREQ=50
*****
** P A S S - 1 **
*****
**
*STEP, AMP=RAMP, INC=1, CYC=10
*STATIC, PTOL=1. 0
0. 00001, 0. 00001
*MODEL CHANGE, REMOVE

```

```
PASS2, PASS3
*END STEP
**
*STEP, AMP=RAMP, INC=100, CYC=10
*STATIC, PTOL=300. 0
0. 004, 6000. 0, 0. 00001, 1000. 0
*TEMPERATURE, FILE=1B, BSTEP=2( INC=1), ESTEP=4( INC=10)
NSP1
*EL PRINT
1, 1, 1, 1, 1, 1
1, 1, 1, 1, 1
1, 1, 1, 1, 1, 1, 1, 1
*NODE PRINT
1, 1, 1, 1, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=10, CYC=10
*STATIC, PTOL=10. 0
0. 00005, 0. 0001
*TEMPERATURE
NSP1, 20. 0
*END STEP
```

H. 3. 2 Pass 2

*RESTART, WRITE, READ, ENDSTEP, STEP=3, INC=2, FREQUENCY=10

** P A S S - 2 **

*STEP, AMP=RAMP, INC=5, CYC=10

*STATIC, PTOL=1. 0

0. 000005, 0. 00001, 0. 000001, 0. 00005

*MODEL CHANGE, INCLUDE

2986, 2987, 2988, 3006, 3007, 3008, 3026, 3027, 3028

*BOUNDARY, OP=MOD

300, 1, 1, -1. 2149E-04

9298, 1, 1, 9. 9963E-03

8298, 1, 1, 2. 2204E-03

7298, 1, 1, -8. 1429E-03

6298, 1, 1, -1. 0414E-02

6300, 1, 1, -1. 0424E-02

6302, 1, 1, -9. 7768E-03

6304, 1, 1, -8. 9320E-03

9298, 2, 2, -3. 9726E-02

8298, 2, 2, -4. 7083E-02

7298, 2, 2, -4. 9075E-02

6298, 2, 2, -4. 7674E-02

6300, 2, 2, -4. 6789E-02

6302, 2, 2, -4. 4840E-02

6304, 2, 2, -4. 2932E-02

*TEMPERATURE

NSP1, 20. 0

NSP2, 0. 0

*END STEP

**

*STEP, AMP=RAMP, INC=5, CYC=10

*STATIC, PTOL=1. 0

0. 00001, 0. 00001

*BOUNDARY, OP=NEW

1000, 1, 2, 0. 0

1025, 2, 2, 0. 0

1575, 2, 2, 0. 0

1600, 2, 2, 0. 0

400, 1, 2, 0. 0

*TEMPERATURE

NSP1, 20. 0

NSP2, 0. 0

*END STEP

**

*STEP, AMP=RAMP, INC=10, CYC=10

*STATIC, PTOL=1. 0

0. 00001, 0. 00001

*TEMPERATURE

NSP1, 20. 0

NSP2, 20. 0

*EL PRINT

1, 1, 1, 1, 1, 1

1, 1, 1, 1, 1

1, 1, 1, 1, 1, 1, 1, 1

```
*NODE PRINT
1, 1, 1, 1, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=100, CYC=10
*STATIC, PTOL=300. 0
0. 004, 6000. 0, 0. 00001, 1000. 0
*TEMPERATURE, FILE=18, BSTEP=6( INC=1), ESTEP=8( INC=10)
NSP1
NSP2
*EL PRINT
1, 1, 1, 1, 1, 1
1, 1, 1, 1, 1
1, 1, 1, 1, 1, 1, 1, 1
*NODE PRINT
1, 1, 1, 1, 1, 1, 1, 1
*END STEP
**
*STEP, AMP=RAMP, INC=10, CYC=10
*STATIC, PTOL=1. 0
0. 000005, 0. 00001
*TEMPERATURE
NSP1, 20. 0
NSP2, 20. 0
*EL PRINT
1, 1, 1, 1, 1, 1
1, 1, 1, 1, 1
1, 1, 1, 1, 1, 1, 1, 1
*NODE PRINT
1, 1, 1, 1, 1, 1, 1, 1
*END STEP
```

H. 3. 3. Pass 3

*RESTART, WRITE, READ, ENDSTEP, STEP=8, INC=2, FREQUENCY=10

** P A S S - 3 **

*STEP, AMP=RAMP, INC=5, CYC=10

*STATIC, PTOL=1. 0

0. 000005, 0. 00001, 0. 000001, 0. 00005

*MODEL CHANGE, INCLUDE

3046, 3047, 3048, 3066, 3067, 3068, 3086, 3087, 3088

*BOUNDARY, OP=MOD

300, 1, 1, -1. 4795E-04

6304, 1, 1, -1. 7128E-02

6304, 2, 2, -5. 3703E-02

6306, 1, 1, -1. 5950E-02

6306, 2, 2, -5. 2309E-02

6308, 1, 1, -1. 4704E-02

6308, 2, 2, -5. 1115E-02

6310, 1, 1, -1. 3220E-02

6310, 2, 2, -4. 8931E-02

7304, 1, 1, -5. 0815E-03

7304, 2, 2, -5. 5596E-02

7310, 1, 1, -1. 3110E-02

7310, 2, 2, -4. 8849E-02

8304, 1, 1, 3. 8948E-04

8304, 2, 2, -6. 0382E-02

8310, 1, 1, -1. 3969E-02

8310, 2, 2, -4. 8729E-02

9304, 1, 1, -1. 3744E-02

9304, 2, 2, -6. 6824E-02

9310, 1, 1, -1. 4718E-02

9310, 2, 2, -4. 8686E-02

*TEMPERATURE

NSP1, 20. 0

NSP2, 20. 0

NSP3, 0. 0

*END STEP

**

*STEP, AMP=RAMP, INC=5, CYC=10

*STATIC, PTOL=1. 0

0. 00001, 0. 00001

*BOUNDARY, OP=NEW

1000, 1, 2, 0. 0

1025, 2, 2, 0. 0

1575, 2, 2, 0. 0

1600, 2, 2, 0. 0

400, 1, 2, 0. 0

*TEMPERATURE

NSP1, 20. 0

NSP2, 20. 0

NSP3, 0. 0

*END STEP

**

*STEP, AMP=RAMP, INC=10, CYC=10

*STATIC, PTOL=1. 0


```
0.00001,0.00001
*TEMPERATURE
NSP1,20.0
NSP2,20.0
NSP3,20.0
*EL PRINT
1,1,1,1,1,1
1,1,1,1,1
1,1,1,1,1,1,1,1
*NODE PRINT
1,1,1,1,1,1,1,1
*END STEP
**
*STEP,AMP=RAMP,INC=100,CYC=10
*STATIC,PTOL=300.0
0.004,6000.0,0.00001,1000.0
*TEMPERATURE,FILE=18,BSTEP=10( INC=1),ESTEP=12( INC=10)
NSP1
NSP2
NSP3
*EL PRINT
1,1,1,1,1,1
1,1,1,1,1
1,1,1,1,1,1,1,1
*NODE PRINT
1,1,1,1,1,1,1,1
*END STEP
**
*STEP,AMP=RAMP,INC=10,CYC=10
*STATIC,PTOL=1.0
0.000005,0.00001
*TEMPERATURE
NSP1,20.0
NSP2,20.0
NSP3,20.0
*EL PRINT
1,1,1,1,1,1
1,1,1,1,1
1,1,1,1,1,1,1,1
*NODE PRINT
1,1,1,1,1,1,1,1
*END STEP
```